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THE JOURNAL OF THE AMERICAN WELDING SOCIETY PUBLISHED FOR THE ADVANCEMENT OF THE SCIENCE AND ART OF WELDING

The Welding Journal

YEAR BOOK NUMBER

Activities of the American Welding Society
Codes, Standards and Research Work
Committees of the American Welding Society
Constitution and By-Laws of the A. W. S.
Alphabetical List of Members
Geographical List of Members
Sustaining Companies

VOLUME 16 DECEMBER 1937 NUMBER 12

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The AMERICAN Welding Journal SOCIETY

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Vol. 16, No. 12 **DECEMBER 1937**

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A Message from Our President

NINETEEN HUNDRED AND THIRTY-SEVEN has been, all things considered, an exceptional year for the AMERICAN WELDING SOCIETY. Its membership has increased, and the Atlantic City Convention was the best and most enthusiastic ever held.

On behalf of the Board of Directors and the Staff, may I hope that Nineteen Hundred and Thirty-Seven has been equally good to you, and express the sincere hope that each of you individually will continue to enjoy throughout the year to come, the best of health, combined with happiness and contentment.

"I heard the bells on Christmas Day
"Their old familiar carols play,
"And loud and sweet
"The word repeat
"Of peace on earth, good will to men."

Longfellow.

ISSUED MONTHLY

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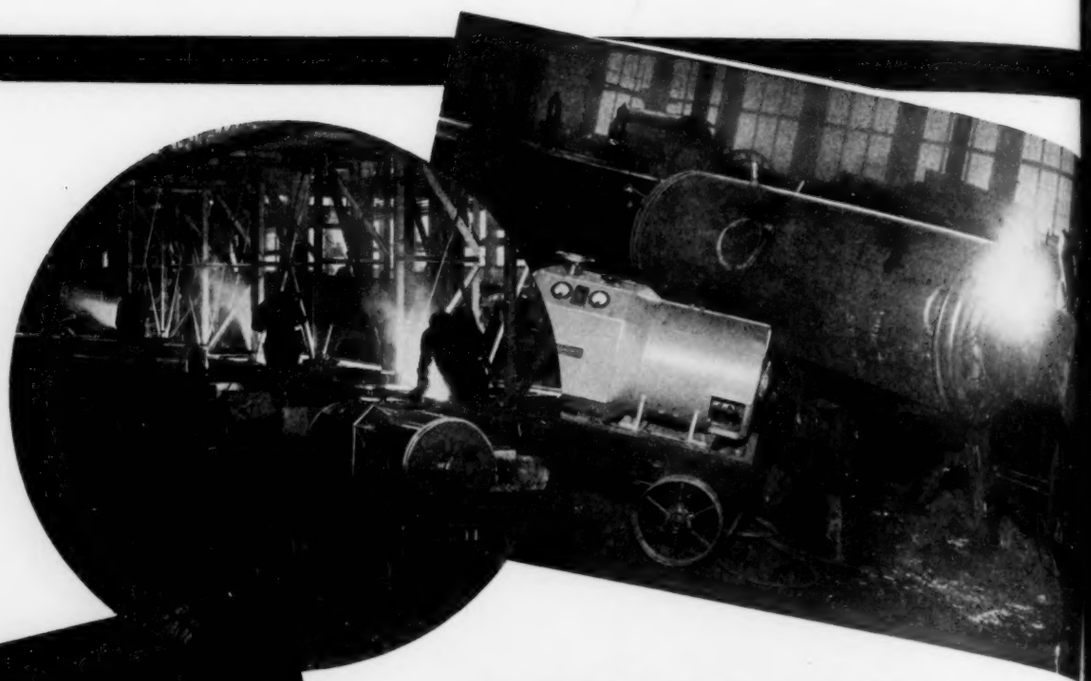
These three views are representative of the hundreds of shops in which WILSON Welders are making arc welding pay a profit. Parts of the 23 ft. x 9½ ft. x 4 ft. Actuator Base shown below, were cut on AIRCO-DB Flame Cutting Machines.



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The WILSON line includes types for every arc welding need.

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The American Welding Society

THE AMERICAN WELDING SOCIETY is a mechanism which has been established for cooperation in matters pertaining to the advancement of welding and the extension of its applications. The Society is an organization of individuals and of various companies and corporations interested in welding by any process, either as users concerned with the efficient application of the art or as manufacturers of welding equipment and supplies interested in its acceptance.

The Welding Industry

Welding is the standard method of joining metal parts in manufacturing, construction, production and maintenance work. Applications range from the welding of small articles, such as watch stems, to the largest metal fabricated structures—ships, locomotives, pipe lines, buildings, bridges, aircraft structures, pressure vessels and machinery of all kinds.

Advancement Through Research

Improvements in welding technique, materials, apparatus and procedures through research during the past decade have been phenomenal. Tensile strengths of the average welded joint have been raised from between 40,000 and 50,000 lb. per sq. in. to from 65,000 to 75,000 lb. per sq. in. With special steels and materials, 100,000 lb. per sq. in. is not impossible. Along with this increase in tensile strength, the ductility of the weld metal has increased from 10 to 30 per cent; the fatigue endurance limit has risen from 10,000 to 28,000 lb. per sq. in.; and impact resistance, as indicated by the Izod test, has jumped from 5 to 50 ft.-lb. or more. Other physical properties have been correspondingly improved. In spite of all these advances in welding, the surface has been merely scratched so that new opportunities are almost limitless in their extent. The solution of many of the complex problems in design, metallurgy and stresses will bring about further economies, improvements of products and the extension of the applications of welding.

Opportunities for Cooperation in the Welding Industry

With the complexity of modern industry it is necessary for people working in the same field to get together, as progress can be made best through such cooperative effort rather than by individual genius.

Advantages to Be Gained Through Membership in the Society

The AMERICAN WELDING SOCIETY is about twenty years old. It started with a membership of a hundred and has grown to nearly three thousand members.

The AMERICAN WELDING SOCIETY offers (1) An opportunity for personal association with the leaders of the industry and exchange of information and ideas. (2) Means to keep informed on the latest developments in the welding field. (3) Opportunity to assist through cooperative effort in increasing the knowledge of welding and extending its applications. (4) Assistance in the development and use of codes and standards. (5) An opportunity through cooperative action to secure material benefits at a minimum cost.

SCOPE OF SOCIETY ACTIVITIES

Some of the aims of the Society may be briefly summarized as follows:

1. To collect and make available authentic and up-to-date information on welding and cutting, and to be recognized as the authoritative source of such information by manufacturers in the welding field, users of the process, engineering societies and legislative bodies.
2. To provide a means for the interchange of knowledge and experience to aid in the solution of the problems of the welding art—technical, ethical and commercial—through cooperative effort.
3. To be the agent of the industry in cooperative research on important technical problems, and to serve as a common spokesman of the industry in matters pertaining to its welfare.
4. To provide opportunities for social intercourse, and thus to promote a better understanding among manufacturers, users and scientists in the welding field, and to foster a spirit of cooperation for the common good.
5. To encourage the development of welding through improvements in present applications and expansion of its uses into new fields, and to develop technical and ethical standards for the welding industry.

Research

Research has always played an important part in the activities of the AMERICAN WELDING SOCIETY. Originally, this work was done by the American Bureau of Welding, a board functioning under the joint auspices of the AMERICAN WELDING SOCIETY and the National Research Council.

More recently, the research work in the welding field has been fostered by the Welding Research Committee under the auspices of the Engineering Foundation and sponsored jointly by the AMERICAN WELDING SOCIETY and the American Institute of Electrical Engineers. The purpose of the Committee is to assemble and digest all available information in regard to welding research and correlate existing and future programs of welding research. Its activities have been divided into three parts as follows:

LITERATURE: Preparing and publishing summaries of the world's literature on welding.

FUNDAMENTAL RESEARCH: Correlating projects of fundamental research carried on in laboratories of technical colleges, giving moderate financial aid in approved cases and publishing results

INDUSTRIAL RESEARCH: Correlating welding investigations, especially of applied problems undertaken by industrial concerns, and publishing results where agreeable to concern in question.

Already many important critical digests of the literature have been made and published. A typical example is a review recently completed in cooperation with the Fundamental Research Committee on Fatigue of Welded Joints. This review involved the critical digest of more than 550 articles appearing in many journals written in several languages. This critical digest represents more than a thousand dollars in time of trained workers hav-

ing available adequate library facilities. The original reports represent, all told, many hundreds of thousands of dollars of research work.

The Fundamental Research now comprises more than 50 active research projects in many universities of the country from the East Coast to the West Coast, and even in Canada. Although some of these researches are obviously of minor importance they serve as a means of educating professors and students of the institution in question, thus providing a supply of young engineers who are familiar with welding. However, a considerable number of these researches have developed data, methods of analysis, and information as to the nature of the phenomena involved, which, all told, constitute a very major contribution to those industries concerned with welding. Altogether more than 100 important reports have been issued.

A cooperative plan makes available to those interested, translations of important welding articles published abroad. Current problems needing investigation are brought to the attention of university workers. Critical digests of research reports published here and abroad are made available to those interested.

Through the stimulation of research reports and the presentation of research papers by the Industrial Research Committee, a number of important investigations made by various industrial laboratories and governmental departments have been made available to industry through the Society.

Reports of all research activities are made available to the members of the AMERICAN WELDING SOCIETY through publication in the JOURNAL of the Society.

Publications

The JOURNAL is the official publication of the AMERICAN WELDING SOCIETY. It has also been accepted as the official organ of the Welding Research Committee of Engineering Foundation. It is necessary for all those interested in welding to read it to keep abreast of the latest information on welding and to have available the latest codes, standards and specifications on all phases of Welding.

Not only does the JOURNAL carry, usually first among publications, all of the technical welding information available, but also a large proportion of its content consists of interestingly written practical articles of popular appeal. It carries departmentals and articles on "Society Activities," "News Items," "Current Welding Literature," "How to Weld It," "Welding Abroad," "Safety Kinks," "Welding Surveys," "Communications" and "Positions Available."

The JOURNAL is published monthly and distributed to all members of the Society without further charge.

All of the important papers presented before the Society and National and Section meetings are published regularly. Each year a Subject and Authors' Index is prepared.

The various issues of the JOURNAL for a single year contain a veritable encyclopedia of up-to-date information on welding.

The Society also publishes a Membership Directory giving the names and addresses of all members.

Meetings

The meetings of the Society, both Section and National, provide another means of keeping informed through the presentation of papers and discussions. There are at present some sixteen Sections of the Society in important cities throughout the country. Many of these hold monthly meetings in season.

The Annual Meeting is held in the fall in connection with the National Metal Congress and Exposition at which the latest developments in welding equipment, materials and applications are exhibited and demonstrated.

One new idea derived from discussion at these meetings may be worth the cost of membership for several years.

Codes and Specifications

The AMERICAN WELDING SOCIETY is recognized as the authoritative spokesman in this country in the matter of welding codes. In many instances these codes are issued in the name of the AMERICAN WELDING SOCIETY and are known as AMERICAN WELDING SOCIETY Codes on Welding. In other instances it has appeared to the Board of Directors and Executive Committee advisable to cooperate with other engineering societies and legislative bodies in the issuance of codes relating to welding. In such instances, it has not always been advisable for the Society to claim undue credit in connection with these codes.

However, in all cases the aim of the Society has been to promote the knowledge of welding and extend its applications with safety and at the same time prevent unjust restrictions being placed on the use of welding.

The first code issued by the American Society of Mechanical Engineers for pressure vessels included some very restrictive measures against welding. The Society, through its research department, carried out an exhaustive series of tests which furnished authentic data to back up the Society in its demands for a more lenient code which would be fair to welding.

These demands led to the appointment of suitable joint committees which through research work and deliberations have resulted in the providing of a new code which permits the use of welding under desirable safety requirements for any pressure or size.

As a matter of fact, in this particular instance the Society was able to take advantage of the national standing of the A. S. M. E. Boiler Code. In many states these rules become laws without further action, and in other instances, they are accepted after due deliberations. The welding industry through the AMERICAN WELDING SOCIETY, in cooperation with the American Society of Mechanical Engineers, has enabled the extension of the use of welding under proper safeguards for boilers and pressure vessels.

Industry as a whole has profited through these arrangements. For example, some chemical and process industries depend for their satisfactory operation upon high pressures usually accompanied with high heats. This requires large vessels of great thickness of material—special steels where fabrication without the use of welding would have been impossible or at great expense.

These rules form the basis of other codes as, for example, the joint A. P. I.-A. S. M. E. Pressure Vessel Code and the Code for Marine Boilers.

In the structural welding field the Society has taken the initiative and has issued a building code which has been adopted by over 150 cities and municipalities. The values have been used in the design and erection of several hundred important welded structures.

In many cities the members of the Society, local sections and special committees have been active in urging the adoption of this code as part of the municipal building codes. Wherever necessary, testimonial evidence has been presented, in some instances, test results, and in still others, continuous consultation with members of local municipal committees and boards.

Codes in general are not permanent unchangeable documents. In welding, particularly, advancement is fairly rapid. It is necessary to keep these codes up to

date and constantly revised. For example, the next revision of the building code will undoubtedly take into account the newer developments in modern covered electrodes and special gas welding wires, which will enable the raising of permissible unit stresses when certain qualification requirements are met. In the same way, the Society maintains an active Conference Committee with the A. S. M. E. Boiler Code Committee to interpret, rivise and enlarge upon existing rules and regulations.

Other codes include Pressure Piping, Code for Fusion Welding and Gas Cutting in Machinery Construction, Specifications for Design, Construction, Alteration and Repair of Highway & Railway Bridges by Fusion Welding, Marine Code for Welding and Gas Cutting, Part D, Tentative Specifications for Filler Metal for Use in Fusion Welding, Standard Tests for Welds, Fusion Welding Symbols, Report on Welding or Cutting Certain Types of Containers Which Have Held Combustibles, Qualification of Welding Operators, and Tentative Rules for the Fusion Welding of Gravity Tanks, Tank Risers & Towers.

In addition to these, there are a number of codes in preparation as, for example, hydraulic piping and oil storage tanks.

Members of the Society, therefore, have a voice in forming the codes under which they work—an advantage which is not to be overlooked.

Standards

The Society has long realized the value of specifications and standards. For example, in the early days of the industry a wide variety of nomenclature and definitions was in vogue. Now, however, by means of standard symbols evolved by the Society, draftsmen are able to specify clearly the type, location and size of welding to be used, thereby eliminating confusion and expense. Other standards developed by the Society include standard tests for welds, standards for arc-welding and resistance-welding apparatus, specifications for welding wire and gages for measuring dimensions of welds.

The rapid growth of welding within the past few years may well be due to the maintenance of quality standards and procedure specifications which have eliminated doubt as to quality of workmanship, and brought industry in general to look upon welding with greater confidence.

Membership Certificate

Each member* of the Society is privileged to display, in office or shop, the certificate. The certificate states that the individual or company is a member of the American Welding Society—an organization devoted to the advancement of the science and art of welding.

Displaying this certificate is good advertising. It lends prestige, it shows you are active in your industry and that you are certain to be up to the minute in every phase of welding.

Headquarters

The Society has its headquarters in the Engineering Societies Building, 25–33 West 39th Street, New York. This is also the headquarters of the major engineering societies of the country.

CLASSES OF MEMBERSHIP

Sustaining Members, being individuals delegated by corporations, firms, partnerships, etc., interested in the science and art of welding, with full rights of membership.

Annual dues **$100.00**

* Except operating and student members.

Members, being individuals not less than 23 years of age who shall have been for at least three years engaged in work having a direct bearing on the art and science of welding and shall have made some contribution to the science and art of welding. Members in good standing of any major engineering society are eligible.

Annual dues **$15.00**

Associate Members, being individuals interested in the science and art of welding with right to vote but not to hold office except in Sections as may be provided for by the By-Laws of the Section.

Annual dues **$10.00**

Operating Members, who, by occupation, are operators of welding or cutting equipment, without the right to vote or to hold office excepting in Sections as may be provided for by the By-Laws of the Section.

Annual dues, United States and Canada **$5.00**
Other countries **$10.00**

Honorary Members, with full rights of membership. Honorary Members shall be persons of acknowledged eminence in the welding profession, or who may be accredited with exceptional accomplishments in the development of the welding art upon whom the AMERICAN WELDING SOCIETY may see fit to confer an honorary distinction.

Student Members, being individuals who are actually in attendance at any one of the recognized colleges and approved trade schools, without the right to vote or to hold office excepting in Sections as may be provided for by the By-Laws of the Section. At the termination of fiscal year of status as a student, affiliation as "Student Member" shall cease.

Annual dues **$2.50**

Classification of Members Interested in American Welding Society

1. Engineers interested in metals, their fabrication, maintenance of plant equipment, or designers who must specify materials, sizes and must know unit strengths of various types of joints for various applications.
2. Production men concerned with lowering costs, improvement of products or speeding up time schedules.
3. Purchasing agents interested in the purchase of the best materials with due consideration to cost and other factors.
4. Draftsmen who need to know the latest symbols, types of welding and methods which are applicable to their work.
5. Presidents of companies concerned with broad economic and technical developments which may affect the operations of their companies.
6. Foremen and welders concerned with the best procedures, methods, techniques and latest developments in the art of welding.
7. Professors and students who desire fundamental training and knowledge in engineering principles and recent developments in welding which may affect design construction or production operations.
8. Research men desiring to keep abreast of the latest developments in science, metallurgy and other practical applications.
9. Inspectors whose duty it is to see that proper welding procedures are followed by qualified welders and in accordance with specifications.
10. Contractors interested in having accurate knowledge as to lowering costs of construction, safety, best materials and procedures and information as to codes, standards and specifications.

Membership Report, Period Ending September 30, 1937

The last Year Book included a report of the membership status as of March 31, 1936. At that time there was a total of 1767 members, whereas, at the present time there is a total of 2541 members. As will be seen from the tables below, the largest number of members are the "Associate" grade with annual dues of $10.00. This classification accounts for approximately 50% of the entire membership. Last year the operating members constituted 18½% of the entire membership; this year the percentage of operating members rose to approximately 23%. There was an increase of 67% in the total number of Sustaining Members of the AMERICAN WELDING SOCIETY.

A considerable number of Sections have been added. Others are in process of formation. A separate table is given showing localities where Sections exist or are in process of formation.

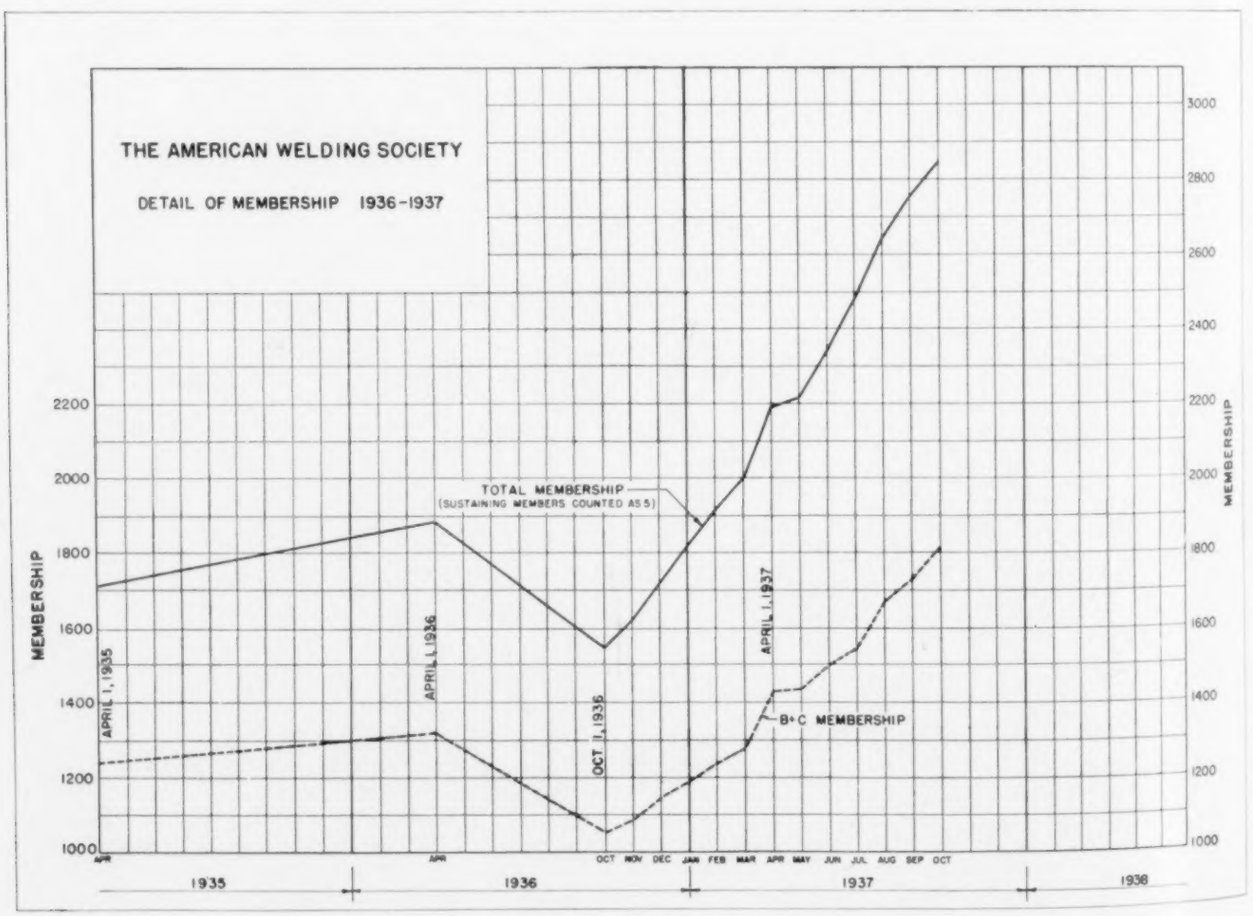
The charts depict the growth of the AMERICAN WELDING SOCIETY in a much clearer fashion than do the tables. Except for a slight drop at the beginning of October 1936 when all delinquents, who were carried through the depression, were dropped at one time, the growth has been steady and continuous.

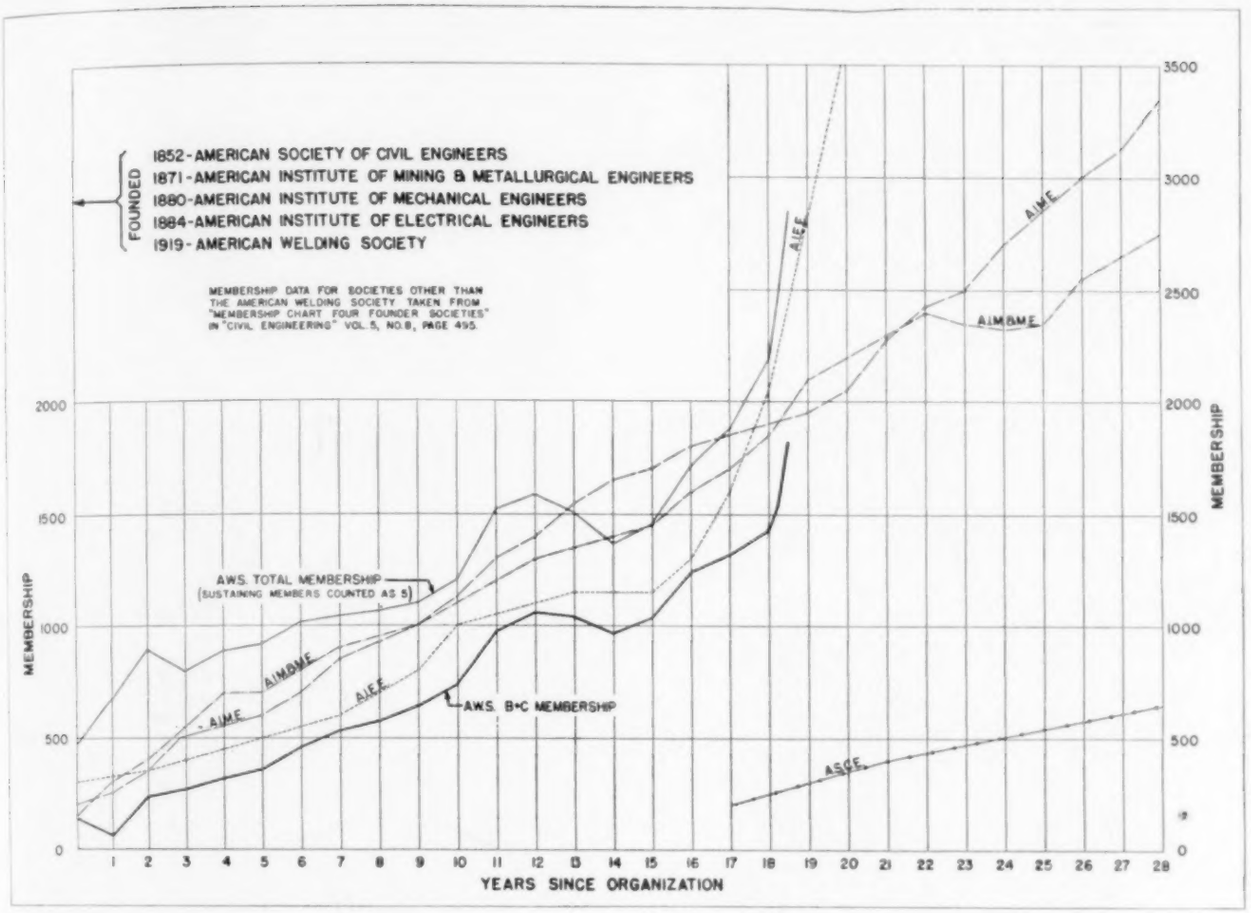
The second chart shows a comparison with the four Founder Societies. Even if only the B and C members are included, the Society's growth is comparable to the four Founder Societies. If all the members are included, with the Sustaining Members counted as five, then the growth of the Society is larger than any of the Founder Societies.

American Welding Society Membership Status of Oct. 1, 1937

	A	B	C	D	E	F	Total
Total Membership, Oct. 1, 1936	45	317	734	253	2	15	1366
Added during year	34	271	623	401	...	37	1366
Dropped during year	3	23	107	50	1	7	191
Net Change during year	31	248	516	351	−1	30	1175
Total Membership, Sept. 30, 1937	76	565	1250	604	1	45	2541*

* Includes 163 delinquent members.




FOUNDED
1852-AMERICAN SOCIETY OF CIVIL ENGINEERS
1871-AMERICAN INSTITUTE OF MINING & METALLURGICAL ENGINEERS
1880-AMERICAN INSTITUTE OF MECHANICAL ENGINEERS
1884-AMERICAN INSTITUTE OF ELECTRICAL ENGINEERS
1919-AMERICAN WELDING SOCIETY
MEMBERSHIP DATA FOR SOCIETIES OTHER THAN THE AMERICAN WELDING SOCIETY TAKEN FROM "MEMBERSHIP CHART FOUR FOUNDER SOCIETIES" IN "CIVIL ENGINEERING" VOL. 5, NO. 8, PAGE 495.
AWS. TOTAL MEMBERSHIP (SUSTAINING MEMBERS COUNTED AS 5)
AWS. B+C MEMBERSHIP
AIME
AIMME
AIEE
ASCE
MEMBERSHIP
YEARS SINCE ORGANIZATION

"Active" Society Membership, October 1, 1937

	A	B	C	D	E	F	Total
Atlanta	..	4	9	1	..	..	14
*Birmingham	..	8	31	8	..	..	47
*Boston	..	20	39	16	..	..	75
Canada	..	16	15	9	..	..	40
Canton	1	4	10	6	..	2	23
*Chicago	5	55	90	31	..	..	181
Cincinnati	1	5	11	4	..	..	21
*Cleveland	10	22	78	30	..	..	140
Colorado	3	6	2	6	..	..	17
*Detroit	5	13	50	25	..	1	94
*Honolulu	..	23	8	7	..	..	38
*Kansas City	..	13	35	44	..	4	96
*Los Angeles	2	11	173	15	..	4	205
*Maryland	1	5	26	8	..	..	40
*Milwaukee	2	5	27	19	..	..	53
*Montana	..	1	17	20	..	..	38
*New York	13	97	124	28	..	3	265
*Northern New York	1	6	20	62	..	19	108
*Western New York	..	6	24	5	..	..	35
*Northwest	1	23	9	16	..	..	49
*Oklahoma City	..	6	17	2	..	..	25
*Philadelphia	12	25	87	17	1	..	142
*Pittsburgh	8	34	44	12	..	..	98
*Portland	..	1	2	5	..	..	8
*San Francisco	2	11	37	18	..	3	71
*St. Louis	..	16	29	9	..	1	55
North Texas	1	3	7	1	..	..	12
*South Texas	1	26	16	18	..	..	61
Tulsa	..	3	4	1	..	..	8
*Washington, D. C.	..	6	16	1	..	..	23
*Youngstown	3	8	12	29	..	2	54
(Not in Sections)	4	62	103	63	..	4	235
Total Membership	76	547	1175	536	1	43	2378†

* Sections organized.
† Does not include delinquent members.

Research Activities

IN former years the research activities of the Society were centered around the American Bureau of Welding, a joint advisory board on research functioning under the auspices of the AMERICAN WELDING SOCIETY and the National Research Council.

More recently, a Welding Research Committee has been organized by the Engineering Foundation, which is being sponsored jointly by the AMERICAN WELDING SOCIETY and by the American Institute of Electrical Engineers.

Each month the Welding Research Committee publishes a report which appears as the Supplement to THE WELDING JOURNAL. This issue contains the Annual Report of the Welding Research Committee and its three divisions on Literature, Fundamental Research and Industrial Research. The Supplement contains personnel and other matters of general interest comparable to the information published by the Society in its Year Book.

All told the work and reports of the Welding Research Committee and its three Divisions during the year constitute major contributions to our knowledge and the advancement of welding. Many of the important universities and leaders in welding research have been drawn in this work. Every member of the Society is urged to carefully study the research supplement to this issue.

Report Activities Meetings and Papers Committee

THE Meetings and Papers Committee has under its general jurisdiction (1) the publication of THE WELDING JOURNAL (2) the technical program of the Annual Meeting and (3) general publicity of the Society. THE WELDING JOURNAL continues to be recognized as the foremost welding journal of the world. During the twelve months of this year there was published a total of 1152 pages as compared with 684 pages for the same twelve months of last year. The arrangements made last year with the Welding Research Committee of Engineering Foundation for the publication of the Research reports of that body as a supplement to THE WELDING JOURNAL, have continued throughout the year and is one of the strongest features of THE WELDING JOURNAL. Several new departments have been added; the most notable of which is the two-page pictorial spread each month depicting progress in a given industry.

The technical program for this year was probably one of the most outstanding in the history of the Society. A day and a half was given over to research reports. Other sessions include fabrication, alloy steels, shipbuilding and railroads. A total of 31 papers were scheduled for presentation at the Annual Meeting in Atlantic City. The publicity work now comes under a special committee and has been removed from the jurisdiction of the Meetings and Papers Committee.

E. VOM STEEG, JR., *Chairman*

Publication of Journal Rules of Procedure

THE Society shall publish and distribute free of charge to its members a monthly publication to be known as the JOURNAL OF THE AMERICAN WELDING SOCIETY, or in abbreviated form, THE WELDING JOURNAL.

The JOURNAL may include editorials, news items, technical reports and papers, employment service bulletin, bibliography of current welding literature, advertising, and other items arranged, from time to time, by the Publications committee. All papers presented at Section and National meetings shall be the property of the AMERICAN WELDING SOCIETY. All Sections and the National Program committee are requested to send copies of all papers presented at monthly Section meetings or National meetings promptly to the Editor of the JOURNAL.

Reports of the Welding Research Committee, technical and other committees of the Society selected for publication shall be published in the JOURNAL. Such reports, however, shall receive the necessary approval of the Welding Research Committee and/or the AMERICAN WELDING SOCIETY before being released for publication. Any member of the AMERICAN WELDING SOCIETY and others interested in welding shall have the privilege of writing technical papers and submitting them for consideration of the Publications committee. Papers selected for publication shall be published at the earliest convenient time in the JOURNAL. Authors submitting papers for publication in the JOURNAL shall not submit them to other publications unless this is clearly indicated in the accompanying letter of transmittal.

The Editor shall advise within twenty days after receipt of papers whether or not they will be published in the JOURNAL. Failure of such notification will allow the author the privelege of submitting the paper to other publications.

Papers presented before National meetings, Sections of the Society, committee reports, and papers dealing with results of research investigation, details as to technique and kindred papers will be favored as to time and space.

Papers presented before joint meetings with other societies shall be treated as special cases and the rights of cooperating organizations shall be fully recognized.

The immediate supervision of the work in handling of publication details shall rest in the hands of an Editor who shall, however, at all times be responsible to the Publications committee. In case of doubt as to policy in regard to editorials, advertising and technical papers, the Editor shall call upon the Publications committee, except that in the case of disagreement, it will be referred to the Executive Committee of the Society.

The Editor shall delete all advertising from papers and an author may not bring in the name of his company more than once in an article and preferably not at all. The title and connection of the author of any paper may be indicated by suitable footnote. All trade names not generally accepted by the Nomenclature Committee may be deleted at the option of the Editor. The author or his company may, however, use the advertising space in a particular issue in which his article appears to bring out advertising points, subject, however, to the general requirements for advertising matter.

Papers dealing with comparisons relating to competitive processes when originating with a manufacturer of equipment shall not be accepted for publication. When such papers originate with "users" they may be accepted if advancing the general aims and objectives of the Society.

The AMERICAN WELDING SOCIETY is primarily concerned with welding, cutting and allied processes. Inasmuch as there are other organizations and publications which cover other fields, articles shall in general deal with welding and cutting matters or their closely allied processes. Articles dealing with the use of welding and/or cutting equipment for other purposes than that indicated above shall in general not be published in the JOURNAL.

Each author may receive up to six copies of the JOURNAL in which his article appears without charge.

The Publications committee may not incur any expense in connection with the publication unless provided for in the annual budget or by the Executive Committee. The Editor of the JOURNAL shall be required to manage publication costs in such a way that the total for the year shall not vary more than 5% from the allotted budget. Modifications in the budget and other special arrangements may be made upon approval of the Finance and Executive committees. The Editor shall be permitted to solicit cooperation from individuals, companies, committees and associations in defraying costs of cuts, drawings or extraordinary printing expenses.

Financial Report

There was published in the May 1937 issue of the JOURNAL the auditor's report for the year ending March 31, 1937. This is reproduced below.

An interim report issued by the auditor on Sept. 30, 1937 showed the following:

Total Current Assets	$4,885.06	
Fixed Assets	3,760.93	
Total Assets		$8,645.99
Total Liabilities		2,883.21
Net Worth		$5,762.78

A report showing receipts and expenditures for the year 1937 will be prepared by the auditor early in January and published in the February issue of the JOURNAL.

AMERICAN WELDING SOCIETY STATEMENT OF CASH RECEIPTS AND DISBURSEMENTS FOR THE PERIOD FROM APRIL 1, 1936 TO MARCH 31, 1937

CASH DISBURSEMENTS

Section Refunds—Membership Dues	$ 4,484.74
Office Equipment	338.71
Journal—Printing	7,848.40
Editorial	1,995.00
Cuts	1,224.99
Miscellaneous	306.22
Postage	672.37
Promotion	293.45
Year Book	250.00
Salaries	8,335.25
American Bureau of Welding	1,241.16
Rent	1,327.84
New York Section Expense	588.01
New York Section Lecture Course	442.98
Postage	948.10
Stationery & Printing	709.51
Committee Expenditures	288.01
Index & Binding	130.79
Petty Cash Fund	75.00
Telephone & Telegraph	251.82
Membership Promotion & Activities	300.53
Travel	66.90
Subscription Refund	11.25
Reprints	290.61
Annual Meeting	1,307.08
General Office Supplies & Miscellaneous Items	618.97
Welding Handbook	304.00
Bound Volumes	4.83
Bulletins & Reports	730.45
Society Emblems	8.38
Resale Books	178.54
	$35,573.89*
Bank Balance, April 1, 1937	5,231.34
	$40,805.23

CASH RECEIPTS

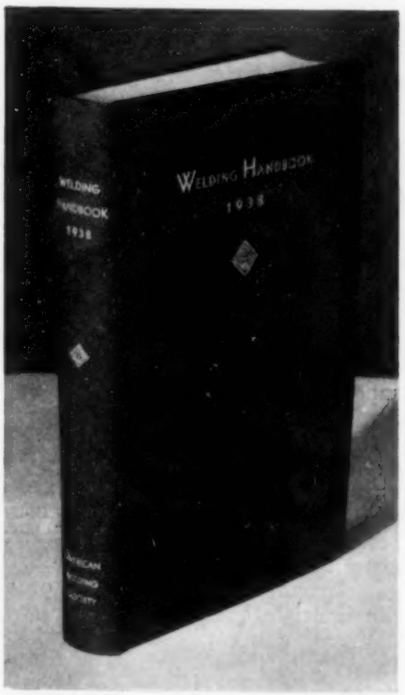
Membership Dues	$20,555.66
Journal—Advertising	6,675.25
Subscriptions	4,507.45
Reprints	387.12
Supplement	263.25
Cut refunds	1,042.29
Annual Meeting	1,630.25
Welding Handbook	304.00
Bulletins & Reports	1,956.07
Bound Volumes	195.90
Journals	101.69
Resale of Books	263.60
Society Emblems & Certificates	20.00
New York Section Lecture Course	870.00
Postage Refunds	29.73
Committee Expense	66.82
Stationery & Printing	9.19
New York Section	31.50
	$38,909.77
Bank Balance, April 1, 1936	1,895.46
	$40,805.23

*Of this amount expenditures during 3 months period Jan. 1, 1937 to March 31 amounted to $11,955.88.

American Welding Society
29 West 39th Street
New York, N. Y.

I HEREBY CERTIFY that the Statement of Cash Receipts and Disbursements for the period from April 1, 1936 to December 31, 1936, presented herewith, is in agreement with the books and records of your Society and compares with the statement of Bankers Trust Co., the depository.

DAVID JOSEPH
Certified Public Accountant



WELDING HANDBOOK

Will be available about March 15, 1938

Prices to Members—Effective to January 10, 1938

Sustaining Members and Members	One Copy Free
Associate Members (Those who joined before Nov. 15, 1937)	One Copy Free
Associate Members (Those who joined after Nov. 15, 1937)	One Copy $2.00
Operating and Student Members	One Copy $2.00
Additional Copies to All Members	$4.00
Price to Non-Members	$4.00

Prices After January 10, 1938

Sustaining Members and Members	One Copy Free
Associate Members, Operating Members and Student Members	One Copy $2.50
Additional Copies to All Members	$5.00 per copy
Copy to Non-Members	$6.00

Technical and Special Committees of the American Welding Society

Committee on Building Codes

APPOINTED January 9, 1928, to assist municipal and state code committees and building departments in preparation of suitable regulatory specifications for fusion welding in fabrication of structural steel, piping and tankage in building construction. It was intended that the committee issue recommendations for welding which have proved to be sound from an engineering standpoint and safe in practical application, and that it endeavor to have modified any unjust restrictions on welding and cutting which may now be imposed. In 1930 it was decided that the committee's activities also include the preparation of a Code for Resistance Welding in Building Construction.

The Committee prepared Code for Fusion Welding and Gas Cutting in Building Construction, Part A—Structural Steel, which was issued in 1928; it compiled data on existing welded structures and cities which have adopted or permitted the use of welding in building codes which was published in the Society's JOURNAL in January 1930; it prepared a second edition of Code for Fusion Welding and Gas Cutting in Building Construction, Part A—Structural Steel, which was issued in 1930; at that time it reported that over 100 cities in the United States permitted welding to be used in building construction, in most cases the codes adopted being identical with that issued by the A. W. S., and in no case were there any important differences.

A third edition of Code 1, Part A, for Fusion Welding and Gas Cutting in Building Construction was published in January 1935.

Code 2 for Resistance Welding of Structural Steel in Building Construction was issued in February 1935. This code restricts the application of resistance welding those members (such as joists, light trusses, stair stringers and similar parts) which, by reason of their shop fabrication in multiple quantities, can be subjected to definite qualification tests. The allowable working stresses coincide with those specified in Code I, and a minimum factor of safety of 3 is required for the weakest member or joint of any product manufactured by resistance welding machines.

The Committee cooperated with the Special Committee concerned with the progress of the new Building Code proposed for New York City. The recent enactment of the Building Code by the City, with recognition of structural fusion welding and of resistance welding for bar joists and similar products will have far-reaching effects in bringing about further acceptance of welding.

The Committee has offered to the Board of Directors proposed revisions of the 1934 edition of Code No. 1. These include bringing the various references to other documents and new A. W. S. Codes up to date, and the adoption of a schedule of higher design stresses for welds made with high strength filler metal together with higher test qualifications for operators using such metal.

The Committee is cooperating with local groups where code revisions, permitting welding, are pending.

Committee on Welding Bridges

Upon completion of its work in the preparation of the Specifications for the Design, Construction, Alteration and Repair of Highway and Railway Bridges by Fusion Welding, issued in April 1936, the original Committee on Welding Bridges was discharged. A new committee for the review and interpretation of the Specifications was authorized by the Executive Committee of the Society in order that proposals for the amplification of the Specifications and questions arising in connection with it might be properly handled.

The new committee is made up of official representatives of interested organizations and is called the Conference Committee on Welding Bridges and is devoting its activities to:

(1) Clarifying points in the specifications which may be questioned as a result of practical application; and

(2) Securing the adoption of the A. W. S. bridge specification by professional bodies throughout the country.

As a result of work along the latter lines, the American Association of State Highway Officials has agreed to adopt the A. W. S. specification with the exception of unit stresses. It has been placed before the American Railway Engineering Association and the American Institute of Steel Construction. The former Body has given the same serious consideration and definitely recommended the A.W.S. specification for adoption at its Annual Meeting in March 1938.

As a result of concerted efforts on the part of the Committee, the AMERICAN WELDING SOCIETY Bridge Specification is being extensively brought to the attention of the engineering profession throughout the United States. It is being tested in actual practice. The lessons of experience are being carefully analyzed and a systematic effort is being made to bring these specifications through subsequent revision, to a state of perfection.

Filler Metal Specifications Committee

Filler Metal.—After seven years of arduous labor, your Committee has brought about the issuance of a specification for Iron and Steel Filler Metal that has been adopted and published jointly by the A.S.T.M. and your Society. This result should be gratifying to the entire welding fraternity.

The Committee will be called upon to consider revisions of the specifications developing from time to time. It will also give consideration to the preparation of a specification for alloy steel filler metal, and to the standardization for coloring ends of various grades of electrodes.

Committee on Welding in Marine Construction

Organized September 24, 1929, to prepare an "American Welding Society Marine Code for Fusion Welding and Gas Cutting;" to report on any questions regarding the use of Welding in Marinine Construction that may be submitted to the Society; to submit a periodical state-

ment on status of Welding in Marine Construction both at home and abroad; and to advise on the making of tests needed by the shipbuilding industry in case they are of a nature that might best be undertaken by an authoritative body, such as the Society.

In July 1930 the Committee was enlarged to include two Subcommittees—one on Boilers and the other on Hulls. The various Subcommittees were authorized to act as conference committees to the Bureau of Navigation and Steamboat Inspection of the U. S. Department of Commerce and assist that body in preparation of specifications for Boilers, unfired pressure vessels, piping, hull structures, and on other matters coming under the cognizance of the various Subcommittees.

The Code as outlined is subdivided as follows:

Part (*A*) General Application
(*B*) Rules for Fusion Welding of Drums and Shells of Marine Boilers and Pressure Vessels
(*C*) Rules for the Repair of Marine Boilers and Pressure Vessels
(*D*) Rules for the Fusion Welding of Hulls and Hull Parts
(*E*) Rules for the Repair of Hulls by Fusion Welding
(*F*) Rules for Fusion Welding of Marine Piping Installations

Part (*B*) has been completed and approved by the Main Committee and the Board of Directors of the Society. These rules are based on rules prepared by the A. S. M. E. Boiler Code Committee in cooperation with the A. W. S. Committee on Welded pressure Vessels. They have been accepted by the Bureau of Navigation and Steamboat Inspection of the U. S. Department of Commerce Committee and have been embodied in Tentative Marine Boiler Rules of this Bureau.

The Boiler Subcommittee is undertaking the preparation of Parts (*C*) and (*F*) for which there is a pressing demand.

Part (*D*) has been completed and published in the February 1935 JOURNAL of the Society. The new rules of the American Bureau of Shipping relating to hull construction have been based on these rules.

The Hull Subcommittee is undertaking the revision of Part (*D*) and the preparation of Part (*E*).

A Subcommittee, "Committee on Thermal Shrinkage and Stresses," has been organized. The objects of this committee are to make a survey of the present welding procedure practices of the various shipyards; to quantitatively determine the transverse and longitudinal shrinkage of various forms of welds and welded joints used in the construction of ship hulls; and to prepare the data obtained in such form that it can be used in the design and construction of ship hulls.

Committee on Nomenclature, Definitions and Symbols

The Committee on Nomenclature, Definitions and Symbols has prepared and offered for adoption revised welding symbols and instructions for their use. After approval by the Board of Directors, these symbols were published in the June 1937 issue of the Society JOURNAL and reprinted in bulletin form. Many copies have already been sold of this new standard.

These revised symbols have been adopted by the various technical bureaus of the United States Navy, and have been offered to the American Standards Association with recommendation for adoption as an American standard. A chart of the major welding processes is practically complete and will be offered shortly for approval and publication.

Work on definitions is being delayed awaiting the chart's approval, as many definitions hinge on the relationship of the various processes to each other.

A "Classification" Subcommittee prepared an indexing system similar to that issued by the American Institute of Architects taking in all welding processes.

Committee on Large Welded Pipe for Hydraulic Purposes

The Committee has prepared a tentative draft of a code which comprises the general ground covered by the existing codes for unfired pressure vessels with the exception of rules for special fittings such as stiffening rings, supports, reinforcings around openings, Y's, Tees and such. These are left purposely in order to develop first the main body of the code, leaving treatment of specials, for a second stage. The first drafts have been submitted to members of the committee and to two or three manufacturers for their comments; as soon as all comments, criticisms and suggestions are received, they will be codified and distributed again to members for their study. Following this, a revised draft will be prepared and distributed in printed form over a still larger field, including all principal manufacturers and users of such pipe. After this distribution, it is expected a final draft can be prepared for provisional adoption.

Committee on Correlation of Code Requirements for Qualification of Operators of Welding Equipment

The Committee worked toward the elimination of confusion by correlating varying qualification tests requirements by merging them into one uniform code, adequate to satisfactorily qualify welding operators working under any and all codes, and using any of the established fusion welding processes. It is the purpose of these qualification tests (1) to adequately determine the quality of a fusion welded joint by the specific process used and, this having been determined, (2) to test the ability of the welding operator to apply that process. After two years of the most intensive study in which all existing welding codes and specifications have been analyzed with a view to determining not only the purpose, but also the method involved in qualifying the operators, the Committee submitted for the Society's approval, a document which it believes gives promise of stabilizing and rendering practical, this rather difficult element of qualifying both the process and the operator of fusion welding. These rules have been published as a supplement to the October 1936 JOURNAL.

This Committee's activities this year have been confined to interpretation and revision work, the principal revision being that of Paragraph 9 of both Parts I and II in order to incorporate definite wording to show form of limiting test requirements that should be imposed by codes adopting these qualification rules. No actual limiting values were incorporated, however, as this is considered to be the perogative of the code-making body adopting the rules.

An important detail of this revision is the adoption of the "Zones of Defects" method of evaluating porosity and inclusions in weld deposits to replace the much used nick-break test.

The Committee is pleased to announce that these Qualification Rules have now been adopted by several Codes, including the A. S. M. E. Unfired Pressure Vessel Code, the A. W. S. Bridge Welding Code, the A. W. S. Code for Welding Oil Storage Tanks and the A. W. S. Gravity Tank Code.

Committee on Standard Tests for Welds

After a thorough study of the subject, the Committee decided its chief function is to aid in harmonizing the

many minor variations now existing between details in the weld testing methods of various codes and specifications—some of a minor nature not greatly affecting results but preventing tests carried out under one specification from being exactly applicable under a second specification; other variations, notably those in the bend test requirement, are of a more basic character.

The committee has concentrated on producing methods of tests for welds that would help eliminate these variations.

As a result of meetings and exhaustive correspondence, a tentative draft has been prepared and submitted to chairmen of committees sponsoring the various other specifications and it is hoped that mutual agreement can be reached on a standard method of tests for welds which can then be submitted to the Board of Directors for ratification.

Whether or not a particular test should be made and the minimum results to be obtained from such test is a function of specific code committees, rather than a function of the Committee on Standard Tests for Welds. For example, it may well be that minimum tensile test requirements would be different for various applications, but the method of making the test should be the same. This will avoid unnecessary duplication and expense. Later on, when opinions become crystallized, other types of tests used in research work will be included by the Committee in its standard tests for welds. First efforts will be confined in standardizing tensile, bend, shear and visual inspection (nick break) tests.

Committee on Rules for Construction of Oil Storage Tanks

Appointed February 7, 1933, to prepare rules covering procedure for welding of underground and basement oil storage tanks. Draft of tentative recommendations of committee which were approved by the Executive Committee of the Society, were furnished to Committee on Flammable Liquids of the National Board of Fire Underwriters. The preparation of standard welding procedure for the construction of oil storage tanks above ground (large-size tanks) was undertaken and the committee was enlarged to include representation from oil and tank manufacturing interests.

After four years of intensive work, the committee completed its report, which was submitted to the Board of Directors at its meeting of September 10, 1937.

Agreement on all details of the report was difficult to obtain. Since the report did not specify any definite amount for the working stresses in the steel plate, merely stating that such stresses should be subject to the dictates of good practice and conform to local statutes, the Board of Directors referred the report back to the Committee with instructions to agree upon a definitely specified working stress.

The Board recommended that a conservative value be adopted for this stress which means that it should be taken at the 18,000 lb. figure rather than the 21,000 lb. amount, which is desired by the petroleum interests. The committee is now endeavoring to obtain agreement on this point.

Committee on Welding Code for Pressure Piping

Appointed April 12, 1928, to prepare a welding code for pressure piping for all applications including some hydraulic, gas, air, oil and refrigeration. It was intended that the committee's first activity should be the preparation of a draft of such code for submission to the Sub-Committee on Welding of A. S. A. Sectional Committee on Pressure Piping Codes by whose invitation the Society undertook the formulation of a draft of suitable material for inclusion in the general codes being developed by the Sectional Committee. It was intended, however, that the Welding Code prepared by our Committee should ultimately be issued as an A. W. S. standard.

Final draft of Code as formulated by our Committee was formerly presented at a public hearing at the 1933 Spring Meeting of the A. W. S. The Code was then approved by the Board of Directors at a meeting on April 28, 1933, when the Chairman of the Committee was instructed to formally transmit the Code to the A. S. A. Sectional Committee on Pressure Piping.

The A. S. A. Code for Pressure Piping in its entirety was issued in July 1935 and contains in Section 5, Chapter 3, the approved Welding Code for Pressure Piping.

It is felt perhaps the Committee may be called upon to consider changes and criticisms that may be advanced by industry after the Code has been in use for some time.

Development of Inspection Method Used in Manufacturing of U-69 and U-70 Pressure Vessels

The Committee has been concerned with the preparation of a specification covering the application of the trepanned plug method, originally suggested by Mr. E. R. Fish of the Hartford Steam Boiler Insurance and Inspection Company. Tentative report was submitted to the Committee for comment and criticism. As a result of replies received, the proposed specification was revised on a compromise basis and the revised draft will be acted on by the Committee.

A. W. S. Conference Committee on Welding with A. S. M. E. Boiler Code Committee

Appointed April 12, 1928, to cooperate with Sub-Committee on welding of Boiler Code Committee, A. S. M. E., for purpose of assisting the Boiler Code Committee in dealing with questions which arise from time to time involving the application of fusion welding to boilers and pressure vessels.

This conference committee has had a year of successful cooperation with the Boiler Code Committee and can report satisfactory results. The consultation during the past year has been mostly on minor details of revisions of the Code which the Boiler Code Committee has made to bring the Code Rules up to date and keep in step with progress in the welding art. This cooperation appears to be essential to the proper development of fusion welding in the boiler and pressure vessel industry.

The Conference Committee has been enlarged by addition of representatives from the Bureau of Engineering, the Bureau of Construction and Repair and the Bureau of Ordinance of the U. S. Navy Department.

Special Committees

Legislative

Appointed April 20, 1932, to deal with all State and Federal Legislative matters involving welding which come to the attention of the Society, particularly those proposals which are detrimental to the best interests of the welding art.

Manufacturers

Appointed September 8, 1930, to consider arrangements for future expositions; handle matters pertaining to exposition and look after welfare of welding exhibitors.

Convention

Appointed April 27, 1932, to correlate activities dealing with Fall Meeting and Exposition matters; composed of following: Chairman, Manufacturers Commit-

tee, representing exhibitors; Chairman, Meetings and Papers Committee, representing technical program; Chairman and Secretary, Section in locality where meeting is held; National Secretary and Treasurer.

Board of Award

Samuel Wylie Miller Medal. Established 1927, by S. W. Miller, to be awarded during his lifetime for any meritorious achievement which, in the judgment of the Board of Trustees of the Medal, has contributed conspicuously to the advancement of the art of gas fusion welding and cutting or the art of electric arc fusion welding and cutting. Annually available, $150. Individuals only, but of any country and of any age, shall be eligible for the award.

Mr. Miller died on February 3, 1929, and with his death the medal ceased to exist. The Society, desiring to perpetuate the medal as a memorial to Mr. Miller, at the 1929 Annual (April) Meeting by resolution established the Samuel Wylie Miller Memorial Medal. The award is made by a Board of Trustees of the Medal in accordance with the following rules and regulations:

1. The Samuel Wylie Miller Memorial Medal shall be awarded annually for any meritorious achievement which, in the judgment of the Miller Medal Board of Trustees, has contributed conspicuously to the advancement of the art of welding and cutting.
2. Individuals only, but of any country and of any age, shall be eligible for the award.
3. The award for any calendar year shall be announced and the medal, together with a suitable certificate, presented at an Annual Meeting following the year for which the award is made.

RECIPIENTS OF MEDAL

SAMUEL WYLIE MILLER MEDAL

Awarded to
C. A. Adams (1927)
J. H. Edwards (1928)

SAMUEL WYLIE MILLER MEMORIAL MEDAL

Awarded to

J. W. Owens (1929)	J. C. Lincoln (1933)
E. H. Ewertz (1930)	C. A. McCune (1934)
F. P. McKibben (1931)	M. H. Hobart (1936)
H. H. Moss (1932)	H. S. Smith (1937)

Lincoln Gold Medal.—In December 1936, the Board of Directors gratefully accepted the generous offer of Mr. J. F. Lincoln, president of the Lincoln Electric Company to donate a medal to be given annually through the AMERICAN WELDING SOCIETY for the paper published in the AMERICAN WELDING SOCIETY JOURNAL and presented before a Section or National Meeting that has contributed most to the development and advancement of welding in the judgment of a committee appointed by the Society. The 1937 award was made to Mr. T. M. Jackson.

Welding Handbook—Editorial Committee

Authorized at A. W. S. Executive Committee meeting April 2, 1935, to cooperate with W. Spraragen as Editor, in the preparation of the Welding Handbook. The duties of those on the Editorial Committee are to establish policies bearing on the Handbook and to settle any points of dispute brought to the Committee's attention.

Committee on Educational Activities

Authorized at Executive Committee meeting November 7, 1935—a Central Coordinating Committee to plan and promote activities of an educational nature.

Committee to Confer with Bureau of Navigation and Steamboat Inspection on Qualification of Welding Operators

Authorized at Directors' meeting September 30, 1935.

International Acetylene Association and A. W. S. Joint Committee on Welding of Sprinler Systems

Authorized at Directors' meeting February 9, 1934, at request of Advisory Engineering Council of the National Board of Fire Underwriters. Tentative Rules for the Fusion Welding of Gravity Tanks, Tank Risers & Towers prepared by this Committee were issued by National Board of Fire Underwriters January 1, 1935.

The Committee has been engaged in a thorough and complete revision of the Rules for Welding of Gravity Tanks, Tank Risers and Towers. Inasmuch as the fabricators of such tanks have joined in urging that these Rules be compiled into a form as nearly as possible identical with that of the Oil Storage Tank Code, the revision work has been delayed somewhat, but it is now progressing satisfactorily.

A. S. M. E. and A. W. S. Joint Committee on Machine Shop Practice

Authorized at Executive Committee meeting June 27, 1933.

Public Relations

THE AMERICAN WELDING SOCIETY has to its credit a great many accomplishments. It is recognized the world over as the leading engineering and scientific organization in the welding field. Its pronouncements carry weight in all technical matters pertaining to welding. Its codes and standards are universally recognized.

In order to properly publicize the work of the Society and bring about a general appreciation of the opportunities that exists through cooperation, there has been appointed a Public Relations Committee. The personnel of this Committee is given elsewhere.

1937 BOUND VOLUME

Bound Volumes of the JOURNAL of the Society for the year 1937 will be shortly available with imitation black leather covers. Contains a wealth of information on the latest developments in welding. Also contains Subject and Authors' Index. Price, $5.00 plus postage to members; $6.50 to non-members.

Committees of American Welding Society

Board of Directors 1937-38

President—P. G. LANG, JR., Engineer of Bridges, Baltimore & Ohio Railroad, Baltimore, Maryland
Senior Vice-President—G. F. JENKS, Chief of Technical Staff, Office of Chief of Ord., U. S. Army, Munitions Bldg., Washington, D. C.
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Term expires 1938
C. A. Adams, Pierce Hall, Harvard University, Cambridge, Mass.
E. Chapman, Lukenweld, Incorporated, Coatesville, Pa.
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F. T. Llewellyn, U. S. Steel Corp., 71 Broadway, N. Y. C.
H. L. R. Whitney, M. W. Kellogg Co., 225 Broadway, New York, N. Y.
*D. S. Jacobus, Babcock & Wilcox Co., 85 Liberty St., New York, N. Y.

Term expires 1939
I. T. Hook, American Brass Company, Ansonia, Conn.
G. A. Hughes, Truscon Steel Company, Youngstown, Ohio
R. E. Kinkead, Consulting Engr., 3441 Lee Road, Cleveland, Ohio
H. S. Smith, Union Carbide Company, 30 East 42nd St., New York, N. Y.
Andrew Vogel, General Electric Co., Schenectady, N. Y.
*J. J. Crowe, Air Reduction Sales Co., 181 Pacific Ave., Jersey City, N. J.

Term expires, 1940
A. M. Candy, Hollup Corp., 3357 West 47th Place, Chicago, Ill.
J. H. Deppeler, Metal & Thermit Corp., 120 Broadway, New York, N. Y.
E. R. Fish, The Hartford Steam Boiler Insp. & Ins. Co., Hartford, Conn.
A. E. Gaynor, J. A. Roebling's Sons Company, 107 Liberty St., New York, N. Y.
L. S. Moisseiff, Consulting Engr., 99 Wall St., New York, N. Y.
*A. E. Gibson, Wellman Engrg. Co., 7000 Central Ave., Cleveland, Ohio

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Chicago, C. J. McGregor, Amer. Steel & Wire Co., 208 S. LaSalle St., Chicago, Ill.
Cleveland, E. R. Benedict, Contract Welders, 2445 E. 79th St., Cleveland, Ohio
Detroit, A. S. Douglass, Detroit Edison Co., 2000 2nd Ave., Detroit, Mich.
Kansas City, C. E. Woodman, Kansas City Bridge Co., 215 Pershing Rd., Kansas City, Mo.
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*A. G. Oehler (1925–26)
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Elsewhere in this Year Book there is given a list of Sustaining Company Members and, of course, an advertising section.

This advertising section is included in the JOURNAL for the benefit of the members. It is true that the revenue derived from these advertising pages makes it possible to issue this monthly JOURNAL which contains all of the latest developments in welding. It is also true that these advertising pages carry equally important messages from the view-point of the user. For the most part they represent the most progressive manufacturers in the welding field. New products are being constantly developed. The frontiers of application are extended.

Every user is respectfully requested to consult these advertising pages. Inquiries addressed to the advertisers will receive the most courteous consideration particularly if THE WELDING JOURNAL or membership in THE AMERICAN WELDING SOCIETY is mentioned.

Constitution and By-Laws

Constitution of the American Welding Society

ARTICLE I

Name

This Society shall be known and designated as the AMERICAN WELDING SOCIETY.

ARTICLE II

Objects

The objects of the Society are:

(*a*) To advance the science and art of welding.

(*b*) To afford its members opportunities for the interchange of ideas with respect to the science and art of welding, and for the publication of information thereon.

(*c*) To conduct research into the science and art of welding, cooperating with other societies, associations and governmental departments for the benefit of the industry in general.

(*d*) To acquire and dispose of property for the purposes aforesaid.

(*e*) To do all other things incidental or conducive to the attainment of the above-named objects, or of any of them.

ARTICLE III

Membership

Individuals, scientific societies, associations or governmental departments interested in welding are eligible for membership in this Society.

ARTICLE IV

Government

The management of the affairs of the Society shall be in the hands of a Board of Directors. The Society for the conduct of its affairs may adopt by-laws, rules and regulations not inconsistent with this Constitution, and may provide methods for amending or repealing such by-laws, rules and regulations different from the method of amending this Constitution.

ARTICLE V

Cooperative Research

In order to further its objects most effectively, the Society shall provide for the formation of a separate body to be known by some appropriate title for the purpose of joining with other societies, associations and governmental departments in cooperative research in welding.

ARTICLE VI

Amendments

This Constitution may be amended by a two-thirds vote of the members voting, expressed orally or in writing, at any Annual Meeting of the Society as set forth in the By-Laws, provided a notice stating the proposed amendment or amendments with the reasons therefor and any known objections shall have been sent to each member at least two weeks prior to said Annual Meeting.

ARTICLE VII

Expulsion of Members

Upon the written request of ten or more members with full rights of membership stating specific reasons therein, any member classifying under Section 2, Article I of the By-Laws may be recommended for expulsion by the Board of Directors. If, at a regular meeting or special meeting of the Board of Directors, it is decided that there is or are presented sufficient reason or reasons for such expulsion, the Board shall notify the accused of the charges against him, by mailing a communication to the address of the accused as it appears in the records of the AMERICAN WELDING SOCIETY. He shall then have the right to present a written defense, and to appear for trial, in person or by duly authorized representative, before a meeting of the Board of Directors, of which meeting he shall be notified at least thirty days in advance. Not less than two months after such a meeting, the Board of Directors shall finally consider the case, and if in the opinion of the Board of Directors the charges have been sustained, the accused may be expelled or suspended for such a period as the Board may determine, or he may be permitted to resign.

By-Laws of the American Welding Society

Adopted March 27, 1919. Amended April 28, 1921, April 14, 1922, January 23, 1924, December 23, 1925, July 20, 1927, March 14, 1931, December 26, 1933, November 4, 1934, May 19, 1935, April 10, 1936, November 18, 1937.

ARTICLE I

Membership

Section 1. Individuals having received the approval of a majority of the Membership Committee shall become members of this Society upon the payment of dues, except in the case of honorary members, who shall be elected by unanimous vote of the full Board of Directors.

Section 2. Membership shall be divided into seven classes:

Sustaining Members, being individuals delegated by corporations, firms, partnerships, etc., interested in the science and art of welding, with full rights of membership.

Annual dues.....$100.00

Members, being individuals not less than 23 years of age who shall have been for at least three years engaged in work having a direct bearing on the art and science of

welding and shall have made some contribution to the science and art of welding. Members in good standing of any major engineering society are eligible.

Annual dues.....$15.00

Associate Members, being individuals interested in the science and art of welding with right to vote but not to hold office except in Sections as may be provided for by the By-Laws of the Section.

Annual Dues.....$10.00

Operating Members, residing in the United States, Canada and Mexico who, by occupation, are operators of welding or cutting equipment, without the right to vote or to hold office excepting in Sections as may be provided for by the By-Laws of the Section.

Annual Dues.....$5.00

Operating Members, residing outside of the United States, Canada and Mexico, who, by occupation, are operators of welding or cutting equipment, without the right to vote or to hold office excepting in Sections as may be provided for by the By-Laws of the Section.

Annual Dues.....$10.00

Honorary Members, with full rights of membership, Honorary Members shall be persons of acknowledged eminence in the welding profession, or who may be accredited with exceptional accomplishments in the development of the welding art upon whom the AMERICAN WELDING SOCIETY may see fit to confer an honorary distinction.

Student Members, being individuals who are actually in attendance at any one of the recognized colleges and approved trade schools, without the right to vote or to hold office excepting in Sections as may be provided for by the By-Laws of the Section. At the termination of fiscal year of status as a student, affiliation as "Student Member" shall cease.

Annual Dues.....$2.50

ARTICLE II

Dues

Section 1. The Annual Dues shall be as follows:

Sustaining Members	$100.00
Members	15.00
Associate Members	10.00
Operating Members (resident)	5.00
Operating Members (foreign)	10.00
Honorary Members (there shall be no annual dues)	
Student Members	2.50

Section 2. All dues shall be payable in advance and shall cover the period of one year from the last day of the month in which a member joins.

Section 3. Funds in excess of those provided by the annual dues, which may be needed for research and other work, shall be secured by voluntary subscriptions to be solicited from its members, or from others specially interested.

ARTICLE III

Organization

Section 1. The AMERICAN WELDING SOCIETY, in pursuance of the objects set forth in its Constitution, provides for the formation of Sections.

Section 2. Not less than twenty-five individuals eligible to membership (any class) in the Society may apply for authorization for the organization of a section.

Section 3. The AMERICAN WELDING SOCIETY will return to each Section fifty per cent of the first $250 received in dues during the calendar year, from members in the Section (except from Student members) and 25% of the remainder.

Section 4. The By-Laws of the Section shall not conflict with any provision of the Constitution or By-Laws of the AMERICAN WELDING SOCIETY.

ARTICLE IV

Officers, Nominations and Elections

Section 1. The Officers of the Society shall be a President, six Vice-Presidents, a Secretary, a Treasurer and thirty-six additional Directors as hereinafter provided. All Officers and Directors must be members of the Society.

Section 2. The President shall be elected by the Society for a term of one year and may be re-elected at any time for a second term of one year, but in no case shall any individual serve in this capacity for more than two consecutive years.

Section 3. A Senior Vice-President shall be elected by the Society for a term of one year and may be re-elected at any time for a second term of one year, but in no case shall any individual serve in this capacity for more than two consecutive years. The United States shall be divided into five geographical divisions and one Divisional Vice-President shall be elected from each such division for a term of two years. These geographical divisions shall be known as New York and New England Division, Middle Eastern Division, Middle Western Division, Southern Division and Pacific Coast Division.

The Divisional Vice-Presidents shall not be eligible for immediate re-election to the same office.

Section 4. The Secretary and Treasurer shall be appointed to office by the Directors for such terms of office as the Directors may determine.

Section 5. The Board of Directors shall consist of:

One representative from each Section, a number elected by the membership at large, the Officers of the Society and three Past-Presidents, all as provided hereinafter.

Each Section at its annual meeting shall elect for the term of one year one Director of the AMERICAN WELDING SOCIETY who may be the Section Chairman.

At each Annual Meeting the Society shall elect five Directors-at-large to serve for a term of three years.

The officers and the three Junior Past-Presidents shall be automatically members of the Board of Directors.

Section 6. Nominations and elections of the Officers shall proceed as follows:

(*a*) The Secretary shall mail on or before the first of June of each year to each member entitled to vote a notice that nominations for Officers must reach the Secretary not later than the first day of July. This notice shall outline the vacancies of elective offices that shall be filled at the next regular election and request nominations for such offices. This notice shall contain a copy of this Section and the list of nominees recommended by the Nominating Committee and information that no name shall appear on the final voting ballot unless proposed by at least 25 qualified members. The notice shall also contain the names of the members of the Nominating Committee.

(*b*) The nomination blank must be placed in a plain envelope and sealed; then inserted in a second envelope and sealed. The voter will then write his name thereon in ink and mail it to the Secretary. The Secretary will

certify to the competence and signature of all voters returning endorsed nomination ballots and will then deliver them to the Nominating Committee.

A nomination ballot without autographic endorsement of the voter written on the outside envelope is defective and shall not be canvassed by the Nominating Committee. The Nominating Committee shall first open and destroy the outer envelopes of all properly endorsed ballots and shall then open the inner envelopes, canvass the ballots and certify the results to the Secretary. If no nominations, other than proposed by the Committee, are made by 25 qualified members, then the Secretary shall treat the nomination ballots as Election Ballots and certify the results to the Presiding Officer at the first session of the Annual Meeting.

(*c*) If a member, not proposed by the Nominating Committee, is duly nominated as provided for in paragraph (*a*) of this Section then the Secretary shall mail on or before the second Tuesday in July of each year to each member entitled to vote a ballot stating the names of the candidates for the several offices falling vacant and the time of the closure of the voting. The voter shall prepare his ballot by indicating by means of a cross opposite the name of each candidate he wishes to vote for. The voter shall enclose said ballot in an envelope and seal same. He shall then enclose the sealed envelope in a second envelope marked "Ballot for Officers," seal same and write his name thereon in ink, for identification. The ballot thus prepared and enclosed shall be mailed or delivered unopened to the Tellers of Election.

The Secretary shall certify to the competence and signatures of all voters. A ballot without the autographic endorsement of the voter written on the outside envelope is defective and shall be rejected by the Tellers of Election. A ballot which has choice indicated for more names than there are offices to be filled is defective and shall be rejected by the Tellers.

Section 7. The voting for the election of officers shall close at 1 o'clock in the afternoon on the last Thursday in August of each year. The Tellers shall not receive any ballot after the stated time for the closure of voting. The Tellers of Election shall first open and destroy the outer envelopes and shall then open the inner ones, canvass the ballots and certify the results to the Presiding Officer at the first session of the current meeting of the Society. The Presiding Officer shall then announce the candidates having the greatest number of votes for their respective offices and declare them elected for the ensuing year.

Section 8. The ballot sheet shall list the names of the candidates proposed by the Nomination Committee and the respective offices for which they are candidates and the names of candidates nominated by letter ballot as provided in Section 6 above. Candidates selected by the Nominating Committee shall be identified by an asterisk.

Section 9. In case of a tie in the vote for any office, the President, or in his absence, the Presiding Officer, shall cast the deciding vote.

Section 10. The term of all Elective Officers shall begin on the adjournment of the Annual Meeting of the Society. Officers shall continue in their respective offices until their successors have been elected and have accepted their offices.

Section 11. Tellers of Election. The President shall, on or before the last Thursday in August of each year, appoint three Tellers of Election of Officers, whose duty shall be to canvass the votes cast, and certify the same to the President, or Presiding Officer, at the first session of the Annual Meeting. Their term of office shall expire when their report of the canvass has been presented and accepted.

ARTICLE V

Duties of Officers

Section 1. In addition to the duties hereinafter set forth, the Officers shall perform such other duties as the Society or Board of Directors shall designate.

Section 2. The President shall preside at all meetings of the Society and of the Board of Directors; he shall have general active management and supervision of the affairs of the Society; shall see that all orders and resolutions of the Board are carried into effect; shall attend generally to its executive business and the supervision and direction of all the other Officers in the proper performance of their respective duties; shall submit a report of the operations of the Society for the fiscal year to the Directors at their last regular meeting preceding the Annual Meeting, and to the members at the Annual Meeting in October and from time to time shall report to the Board all matters within his knowledge which the interests of the Society may require to be brought to their notice; shall be, *ex-officio*, a member of all standing committees; and shall have the general powers and management usually vested in the office of the President of a Society.

Section 3. The Senior Vice-President shall be vested with all the powers, and required to perform all the duties of the President in his absence.

Section 4. The five Vice-Presidents shall be the official representatives of the Society in the divisions so elected. They are responsible for the general promotion of the Society in their divisions, that is, for the activities of the present Sections and the organization of new sections.

They are expected to attend as many meetings of sections in their divisions as they can conveniently.

Section 5. The Secretary shall be the Executive Officer of the Society under the direction of the President and Board of Directors or their duly appointed agent. The Secretary shall attend all meetings of the Society and Board of Directors and record the proceedings thereof. The Secretary shall collect all moneys due the Society and deposit the same in depositories designated by the Board of Directors, reporting such deposit to the Treasurer. The Secretary shall conduct the correspondence of the Society and shall keep full records thereof. Under the President and Board of Directors, the Secretary shall be in responsible charge of all property of the Society. With the approval of the Board of Directors the Secretary shall engage such employees as may be necessary and shall be responsible for the work of all employees of the Society. The Secretary shall perform such other duties as may be assigned. The Secretary shall devote his entire time to the affairs of the Society, unless otherwise authorized by the Board of Directors. The Secretary shall be required to furnish bond for the faithful performance of his duties in an amount to be decided by the Board of Directors.

Section 6. The Treasurer shall keep full and accurate accounts of receipts and disbursements in books belonging to the Society, and shall deposit all moneys and valuable effects in the name, and to the credit of the Society, in such depositories as may be designated by the Board of Directors. All checks shall be signed by the Treasurer. He shall disburse the funds of the Society as may be ordered by the Board, taking the proper vouchers to such disbursements, and shall render to the President and Directors at the meeting of the Board, or whenever they may require it, an account of all its transactions as Treasurer, and of the financial condition of the Society, and at the last regular meeting of the Board preceding the Annual Meeting of the Society, a like report for the

preceding year. He shall give the Society a bond in a sum and with one or more securities as required by and satisfactory to the Board for the faithful performance of the duties of his office, and the restoration to the Society in the case of his death, resignation or removal from office of all books, papers, vouchers, money or other property of whatever kind in his possession belonging to the Society.

ARTICLE VI

Board of Directors

Section 1. The Board of Directors shall have the power:

(*a*) To manage the affairs of the Society, except as otherwise provided by law, or by these By-Laws.

(*b*) To purchase or otherwise acquire for the Society any property, rights and privileges which the Society is authorized to acquire, at such prices and on such terms and conditions and for such considerations as they think fit.

(*c*) To appoint Executive Officers and fix their salaries.

(*d*) To determine who shall be authorized to sign, on behalf of the Society, notes, receipts, acceptances, endorsements, checks, releases, any and all contracts and other documents, and shall make such authorization.

(*e*) To perform such other acts as may be necessary to carry out the purpose of the Society.

Section 2. In each case of vacancy occuring in the Board of Directors through death, resignation, disqualification, or other cause, the remaining Directors, by letter ballot, may elect, or appoint a successor to hold office for the unexpired portion of the term of the retiring Director; and in the event that a quorum of the Board of Directors does not exist, the affirmative vote of a majority of the remaining Directors shall become effective. However, if the retiring Director has been elected by a Section, such Section shall furnish a successor.

ARTICLE VII

Committees

Section 1. With the approval of the Board of Directors, the President shall appoint the following committees:

(*a*) An Executive Committee, which shall have such duties as may be prescribed by the Directors to facilitate their work and such power of the Directors as they may delegate to it from time to time.

(*b*) A Finance Committee, consisting of members of the Board of Directors, which shall pass upon all expenditures, prepare the annual budget and have general charge of the finances of the Society. The Treasurer shall be Chairman of the Finance Committee.

(*c*) A Membership Committee of five or more whose duties shall be to formulate plans and prepare literature for increasing the membership of the Society, to pass upon applications for membership in the Society and handle such other matters as may be duly assigned to it.

(*d*) A Meetings and Papers Committee which shall plan and make arrangements for the meetings of the Society. It shall also solicit and pass upon all papers offered for presentation to the Society, and determine which of the papers presented at the Section meetings shall be published in the proceedings. It shall also have power to edit papers before publication.

(*e*) A Nominating Committee of seven members. Announcement of the appointment shall be made by the President through publication in the JOURNAL on or before March 1st. The Committee shall consist of a chairman who shall be a Past-President of the Society and six members (not members of the Board of Directors), one of whom shall be a Past-President of the Society. The Committee shall deliver to the Secretary in writing on or before the last Tuesday in May the names of its nominees for the various elective offices next falling vacant together with the written acceptance of each nominee.

(*f*) A Revision of By-Laws Committee comprising a Chairman (member of Board of Directors) and two other members. This Committee shall receive suggestions for revision of the By-Laws and be responsible for the preliminary wording of such revisions. It shall see that the By-Laws of Sections of the Society shall not conflict with any provision of the Constitution and By-Laws of the AMERICAN WELDING SOCIETY. This Committee shall be responsible for obtaining the signatures of 10 members to any petition for revision as specified under Article XIII and shall transmit such petition or petitions to the Secretary to be presented at a Board of Directors' meeting as specified under Article XIII.

(*g*) A Committee on Code of Principles of Conduct consisting of five members comprising a Chairman, (member of Board of Directors) and four additional members. In addition to the function of formulating and administering a Code, it shall be the duty of the Committee to advise inquirers regarding questions of proper conduct, and to examine into and investigate any practice of any member of this Society which shall be regarded as prejudicial to the welfare of the Society, and report its actions and recommendations to the Board of Directors, which shall take such action as it may deem proper.

(*h*) A Committee or Committees for a definite stated purpose or purposes may be proposed by any member of the Society in good standing together with the personnel of such a Committee or Committees but the final selection of the personnel must be by a majority vote (of the quorum) at one of the meetings of the Society as set forth under Article VIII, Section 1, provided the necessary quorum, per Article VIII, Section 3, is present and further provided that definite notice stating all purposes and objectives of such Committee or Committees shall have been sent to each member, and/or published in the JOURNAL OF THE AMERICAN WELDING SOCIETY for the preceding month, so as to reach the membership at least two weeks prior to said meeting at which the Committee or Committees will be proposed and elected.

(*i*) With the approval of the Board of Directors the President may appoint additional Committees for a *definite stated purpose or purposes.*

Section 2. An Advisory Committee to serve in an advisory capacity when called upon by the Board of Directors. This Committee is automatically perpetuating and is comprised of all Past-Presidents who are not members of the Board of Directors. The Junior Past-President shall be Chairman. In the absence of the Chairman the second, third, etc., Junior member of the Committee shall act as Chairman.

ARTICLE VIII

Meetings and Notices

Section 1. There shall be an Annual Meeting of the Society each year for the election of Officers and other business, held sometime in the Fall of the year at such time and place as may be decided by the Board of Directors. Other meetings may be called at the discretion of the Directors.

Section 2. Notices of all meetings of the Society shall be mailed to the members at least 30 days prior to the date of such meeting, and to the Board of Directors at least two weeks prior to the date of their meeting, and shall contain the time and place of the meeting and the principal business to come before it.

Section 3. Twenty members entitled to vote shall constitute a quorum at meetings of the Society and ten Directors shall constitute a quorum at meetings of the Board of Directors.

ARTICLE IX

Voting and Proxies

Section 1. Voting by proxy shall not be allowed at any meeting of the Society or its Board of Directors or of any of its Committees except by a personal representative presenting written credentials at the particular meeting stated.

ARTICLE X

Welding Research

Section 1. The AMERICAN WELDING SOCIETY shall provide the means for cooperative research in welding by creating or joining in the creation of a research department to be known as the Welding Research Committee. Other societies, associations and governmental departments may be asked to assist in this work.

Section 2. Funds for the work of the Welding Research Committee shall be secured by voluntary contributions, by solicitation or appropriated from the regular funds of the Society. Solicitation of any funds in the name of the AMERICAN WELDING SOCIETY may, however, only be done with the approval of the Board of Directors.

Section 3. Custodian of funds. The custodian of Research Funds shall be the Engineering Foundation or the AMERICAN WELDING SOCIETY or any other body designated by the Board of Directors.

Section 4. Publications. Publications of the Welding Research Committee will in general be printed in the JOURNAL of the AMERICAN WELDING SOCIETY. A separate section of the JOURNAL may be created for the publication of the reports.

ARTICLE XI

Permanent Office

Section 1. A permanent office of the Society shall be maintained in the City of New York and shall be located in the Engineering Societies' Building at 33 West 39th Street, or at such other place as the Directors may determine.

ARTICLE XII

Parliamentary Rules

Section 1. Roberts' Rules of Order shall be the governing parliamentary law of the Society in all cases not definitely provided for by its Constitution or its By-Laws or its own rules.

ARTICLE XIII

Amendments

Changes in these By-Laws may be made by petition signed by ten or twelve voting members of the Society and approved by not less than two-thirds of the Directors present at a meeting regularly called for consideration of the same. Such changes shall be considered temporary and at the expiration of 60 days after proper letter ballot has been sent to the members of the Society shall be considered permanent, unless rejected by 20 per cent of the voting membership.

Rulings Adopted by Board of Directors

April 1920

Powers of Executive Committee—The Board of Directors delegated to Executive Committee its full power with understanding that Executive Committee will immediately report by letter to every member of the Board any action which it has taken and allow sufficient time for objection to such action before it is put into effect. Should a majority of the Board express objection within a week to any action, the President will then immediately call a special meeting of the Board and have the matter presented to them for confirmation.

Section Organization—The Officers of the Society are authorized to assist in formation of sections in any part of the United States under the adopted standard form of Constitution with variations to meet local conditions and to grant the necessary authority for the formation of such sections. When possible, the sections should be affiliated with the local general engineering society and where practicable, the organization work should be conducted from such society headquarters.

NOTE: It has usually been found inexpedient to authorize a Section unless there are at least 15 members in the Sustaining and/or Member grades.

January 1922, and revised December 1927

Delinquents—A delinquent member shall be carried on the rolls of the Society for a period of six months for the first two months of which he shall be supplied with the Proceedings and other literature, but that following the second month, he shall receive only notices of meetings.

Billing Membership Dues—Each member shall be sent a bill on the first day of the month in which his dues become due and a second bill shall be sent one month later stamped with the words "Final Notice, You Will Not Receive Publication if Your Dues Are Not Paid."

October 1922

Refund to New Sections—No money shall be turned over to a newly organized Section on dues received previous to the establishment of the Section from members located in that particular Section.

February 1923

Plans for establishing Sections of the AMERICAN WELDING SOCIETY which received approval at the annual

meeting of the Society in April 1923, are available at National Headquarters. These embrace all steps to be followed under the headings given below:

1. Extracts from Constitution and By-Laws of Society relating to Section Organization.
2. Preliminary Meeting—Notice; order of business; application to National Organization for authority to operate as Section; suggestions to nomination committee.
3. Organization Meeting—Notice; order of business.
4. Method of Conducting Section Work—Affiliation with some engineering body so as to provide meeting place for monthly meetings and in that way give the Section a better technical standing; standard method for handling applications, financial records and reports.

May 1926

A Year Book shall be published as the December issue of the JOURNAL and bound in the same paper cover as the JOURNAL. It shall contain the Constitution and By-Laws of the Society, rulings adopted by the Board of Directors, rulings governing the Meetings and Papers Committee, description of Society, its functions, publication, relation to industry; value of membership; how to become a member; description of Bureau of Welding and its activities, technical and special committees of Society; personnel of Society committees; Membership list (alphabetical and geographical) and list of "Sustaining Companies" with a description of their products.

January 1927

Executive Committee Meetings—Regular monthly meetings of the Executive Committee of the Society shall be held on the afternoon of the first Tuesday of the month, if convenient to members of the Committee.

December 1928

Routine for handling questions referred to A. W. S. Conference Committee on Welding by A. S. M. E. Boiler Code Committee. All replies prepared by the A. W. S. Conference Committee on questions relating to welding matters submitted to Conference Committee by A. S. M. E. Boiler Code Committee shall be approved by Executive Committee of Society, and in cases where questions referred to Conference Committee deal with matters of a legal nature, replies must be approved by Board of Directors, before submittal to A. S. M. E. Boiler Code Committee.

January 1929

Replies prepared by A. W. S. Conference Committee for A. S. M. E. Boiler Code Committee shall be published in A. W. S. JOURNAL after replies have been approved by A. W. S. and A. S. M. E., and if publication of replies meets with approval of A. S. M. E.

February 1932

Advisory Committee of Past-Presidents—An Advisory Committee of Past-Presidents, not members of Board of Directors, shall have the privilege of attending and voting at all meetings of Executive Committee and Board of Directors.

March 1932

Meetings and Papers Committee—In determining the suitability of a subject for presentation at Society meetings, the Meetings and Papers Committee shall be governed by the beneficial results to be obtained from the paper rather than whether or not the subject is controversial; where the Meetings and Papers Committee is in doubt regarding a subject of importance, the matter shall be referred to the Executive Committee for ruling thereon.

December 1935

Listing under "Sustaining Companies" in the Year Book shall be limited to companies supporting the Society to the extent of $100 in dues of "Sustaining Member" or the equivalent in dues of "Members" and "Associate Members," or through advertising in the Society JOURNAL.

A Commission Allowance on Sustaining Members—A Section is allowed a commission of 25% on the first annual dues of a Sustaining Member outside its territory, when the membership is secured by an Officer of the Section.

October 1936

It was voted that the Welding Handbook Editorial Committee be instructed to proceed with the publication and issuance of the Welding Handbook at the earliest possible moment.

May 1937

Executive Committee voted that arrangements proposed by the Welding Research Committee for sharing publication cost of Engineering Foundation Welding Research Committee reports be accepted.

It was voted that the charges of the Handbook be $4.00 prepublication; $5.00 additional copies to members; $6.00 list price after publication, with 20% discount to booksellers.

July 1937

Executive Committee — JOURNAL be made available to libraries and others not eligible to membership in the AMERICAN WELDING SOCIETY at $5.00 in United States and possessions; $5.50 in Canada and foreign countries; single copy $1.00. To those eligible for membership the price shall be $1.00 a copy or $12.00 a year. Additional copies may be obtained by members at 50¢ each.

HOW TO BECOME A MEMBER

An application blank will be sent on request. Fill it out and return to AMERICAN WELDING SOCIETY accompanied by check, money order or cash for first annual dues.

Catalogue of Membership

November 15, 1937

Honorary Member

Adams, Comfort A., The Edward G. Budd Mfg. Co., Philadelphia, Pa.

LIST OF MEMBERS OF ALL GRADES

Arranged Alphabetically

EXPLANATORY NOTE

The following alphabetical list contains the names of members of all grades. The letter in parentheses immediately after each name indicates the class to which the member belongs. The following letters have been used to classify the different grades of membership: A—Sustaining Members; B—Members; C—Associate Members; D—Operating Members; E—Honorary Members; F—Student Members. Where members desire to have their mail sent to their residence the home address is given and is preceded by (Res.). A list of members showing the geographical distribution follows the alphabetical list.

A

Abbott, Lester E. (B), Bell Telephone Labs., 463 West St., New York, N. Y.

Abel, S. T. (C), 3620—13th Ave. North, Birmingham, Ala.

Aborn, R. H. (C), Asst. Metallurgist, U. S. Steel Corp. Research Lab., Kearny, N. J.

Achard, Jean (C), 252 Faubourg—St. Martin, Paris X, France.

Acker, L. E. (B), J. B. Klein Iron & Fdy. Co., Oklahoma City, Okla.

Adams, C. A. (E), Consulting Engineer, The Edward G. Budd Mfg. Co., Philadelphia, Pa.

Adams, C. H. (C), Salesman, Air Reduction Sales Co.; (Res.) 125 Edgewood Road, Towson Estates, Towson, Md.

Adams, Gale (B), Welding Supervisor, Goodman Mfg. Co., 4834 So. Halsted St., Chicago, Ill.

Adams, Julien W. (C), Supt. of Shops, Goslin Birmingham Mfg. Co.; (Res.) 209 No. 52nd St., Birmingham, Ala.

Adams, M. P. (B), 4175-A De Tony Ave., St. Louis, Mo.

Adank, J. L. (C), 1101 Ralph Terrace, Richmond Heights, Mo.

Addis, Wm. H. (B), General Foreman, Welding, Heat Treating & Tool Making, Cameron Iron Works Inc., P. O. Box 1212, Houston, Texas.

Addison, James (B), Edward Wood & Co., Ltd., Ocean Ironworks, Manchester 17, England.

Adelson, J. S. (C), Steel and Tubes, Incorporated, 224 East 131st Street, Cleveland, Ohio.

Adelson, Samuel S. (C), 1451—163rd St., Beechhurst, L. I.

Adolphsen, A. W. (C), Puritan Compressed Gas Corp., 2012 Grand Ave., Kansas City, Mo.

Ahlston, A. C. (C), Chief Mech. Engr., Victorian Railways, Spencer St., Melbourne C 1, Australia.

Ahlstrom, Walter (C), General Foreman, Construction Shops, Commonwealth Edison Co., 2233 S. Throop St., Chicago, Ill.

Aitchison, R. B. (B), Division Process Service Representative, The Linde Air Products Co., 30 E. 42nd St., New York City.

Akins, C. M. (B), Marquette Mfg. Co., Minneapolis, Minn.

Aldrich, Wm. (B), Representative, Metal & Thermit Corp., South San Francisco, Calif.

Alexander, A. J. (C), R. 1, Box 567, Redondo Beach, Calif.

Alexander, Chas. E. (C), Alexander Welding Co., 461 E. Main St., Patchogue, N. Y.

Alexander, F. J. (C), Structural Engineer, Southern California Gas Co., 1700 Santa Fe Ave., Design Engrg. Dept., Los Angeles, Calif.

Alexander, G. (D), Bethlehem Steel Co., Johnstown, Pa.

Alhart, C. G. (D), 724 W. Cottage Ave., Houston, Tex.

Allen, Clayton M. (C), Const. Supt., Los Angeles Bureau of Power & Light, P. O. Box 240, Arcade Annex, Los Angeles, Calif.

Allen, Guy F. (C), Structural Engineer, The Detroit Edison Co.; (Res.) 220 California Ave., Royal Oak, Mich.

Allen, J. R. (D), 107 So. Mercer St., Greenville, Pa.

Allen, Leslie (B), Manager of Works, Bethlehem Steel Co., Oliver Bldg., Pittsburgh, Pa.

Allen, Lloyd, Jr. (C), Dept. of Water & Power, Bureau of Power & Light, 207 So. Broadway, Los Angeles, Calif.

Allen, Wm. G. (C), 5358 Seeboldt, Detroit, Mich.

Allina, A. (C), Alpha Tank & Sheet Metal Mfg. Co., 3883 Delor St., St. Louis, Mo.

Allison, Edgar T. (C), Sinclair Ref. Co., Pipe Line Dept., Carrollton, Mo.

Allison, J. W. (B), Mgr. Welding, Middle Atlantic Dist., Westinghouse Elec. & Mfg. Co.; (Res.) Apt. C, 139 Rutgers Ave., Swarthmore, Del. County, Pa.

Ames, Ethan E. (C), John A. Roebling's Sons Co., 701 St. Clair, N. E., Cleveland, Ohio.

Ames, M. S. (C), % Leeds & Northrup Co., 4901 Stenton Ave., Philadelphia, Pa.

Ames, W. B., Jr. (C), 618 W. Buffalo, Ithaca, N. Y.

Amoneno, Ernest (C), 1117 Cherry, Kansas City, Mo.

Amoss, John K. (C), Engr. Expt. Station, Annapolis, Md.

Anchor, Chas. J. (C), Shop Engineer, American Car & Fdy. Co., Arch Street Office, Milton, Pa.

Anders, Louis H. (C), P. O. Box 211, Birmingham, Ala.

Anderson, Alex. A. S. M. (B), Papaikou, Hawaii.

Anderson, C. S. (D), Penn. Furnace & Iron Co., West St., Warren, Pa.

Anderson, D. C. (B), 401 N. Broad St., Philadelphia, Pa.

Anderson, E. D. (C), District Manager, The Lincoln Electric Co., 1818 Main St., Kansas City, Mo.

Anderson, Edward (C), 1513 Cota Ave., Torrance, Calif.

Anderson, F. A. (C), Engrg. Dept., Fairbanks, Morse & Co.; (Res.) 190 E. Michigan Ave., Three Rivers, Mich.

Anderson, G. M. (C), Chicago Rapid Transit Co., 72 W. Adams St., Rm. 1240, Chicago, Ill.

Anderson, H. (C), Asst. Foreman, Whiting Corporation; (Res.) Route 3, Chesterton, Ind.

Anderson, James C. (D), 2815 Albany St., Schenectady, N. Y.

Anderson, James E. (D), 1019 W. Water St., Lewistown, Mont.

Anderson, James L. (B), Manager, Eng. Research Dept., Air Reduction Sales Co., 181 Pacific Ave., Jersey City, N. J.

Anderson, Karl H. (D), 2111 Myra St., Jacksonville, Fla.

Anderson, P. J. (B), The Buda Co., Harvey, Ill.

Anderson, Robert L. (C), U. S. Navy Dept., Bureau of Yards & Docks, Arlington, Va.

Anderson, V. B. (C), 644 East Florence Ave., Los Angeles, Calif.

Anderson, Walter (B), Vice-President, Taylor Winfield Corp., 14307 Third Ave., Detroit, Mich.

Anderson, Wm. C. (C), 709 Musselshell St., Fort Peck, Mont.

Andre, Frank V. (C), 387 Hodge St., Blairsville, Pa.

Andrews, Chas. (B), Washburn Company, Andrews Division, Rockford, Ill.

Andrews, F. H. (C), 80 Raritan Road, Linden, N. J.

Angus, James (D), Weirton Steel Works, P. O. Box 552, Hollidays Cove, W. Va.

Anspaugh, Eldon (D), 315½ W. Main St., Montpelier, Ohio.

Archer, Fred C. (C), Lincoln Electric Co., 1560 W. Pierce St., Milwaukee, Wis.

Archer, W. E. (B), News-Correspondent, Pacific Coast and Midwest Representative, Welding Engineer Pub. Co.; (Res.) 435 Del Rey Ave., Pasadena, Calif.

Arens, E. G. (C), 107 Mary St., Orillia, Ontario, Canada.

Armour, L. E. (D), 122 N. Second St., Greenville, Pa.

Arness, O. J. (D), 129 S. Parkside Ave., Chicago, Ill.

Arness, W. B. (C), Rustless Iron & Steel Corp.; (Res.) 3400 E. Chase Street, Baltimore, Md.

Arnott, David (A), American Bureau of Shipping, 20 Old Slip, New York, N. Y.

Aschenbrenner, F. J. (B), Chief Metallurgist, Wilson Welder & Metals Co., Box 96, North Bergen, N. J.

Ashley, Charles (D), Ashley Welding Works, Henry & Sterling Streets, Kingston, N. Y.

Athern, W. (D), Brown-Strauss Corp., Box 78, Kansas City, Mo.

Atherton, A. W. (D), 2239 Mascot, Wichita, Kansas.

Augustson, C. G. (D), Car Dept. Welder, N. Y. C. R. R.-Collinwood; (Res.) 636 East 130th St., Cleveland, Ohio.

Aumiller, W. L. (D), 2030 Avenue B, Schenectady, N. Y.

Aurand, Frank E. (B), Field Engineer, Commonwealth Edison Co., 2233 Throop St., Chicago, Ill.

Austin, Frank R. (D), 1505 Central Ave., Augusta, Ga.

Austin, Herbert G. (C), Dist. Mgr. Sales New Eng., Lukenweld, Inc., 131 State St., Boston, Mass.

Axline, R. A. (C), Metallizing Engrg. Co. Inc., 44 Whithall St., New York City.

Axtell, A. C. (D), Lincoln Electric Co., 330 W. 42nd St., New York, N. Y.

Axtell, T. H. (C), 3965 Jennings Road, Cleveland, Ohio.

Ayers, F. E. (D), 2448 Boulder St., Los Angeles, Calif.

Azevedo, John (B), P. O. Box 63, Pahala, Hawaii.

B

Bagsar, Dr. A. B. (C), Sun Oil Co., Marcus Hook, Pa.

Bagwell, M. W. (C), General Steel Tank, Box 552, Birmingham, Ala.

Bailey, H. E. (C), Salesman, Victor Welding Equipment Co.; (Res.) 3821 Santa Fe Ave., Los Angeles, Calif.

Bailey, O. A. (B), 1305 W. 105th St., Chicago, Ill.

Bailey, Robert P. (B), Supt. Service Shop, General Electric Co., 5950 3rd Ave., Detroit, Mich.

Bainbridge, C. G. (B), British Oxygen Co., Ltd., N. Circylar Road, Cricklewood, London, N.W. 2, England.

Baines, Harry C. (D), Welder, Mack Mfg. Co.; (Res.) 1234½ Tilghman St., Allentown, Pa.

Baker, H. U. (B), Engineer, Box 64 Station A, Los Angeles, Calif.

Baker, T. R. (B), Welding Engineer, Steel Sales Corp., 4067 Park Ave., St. Louis, Mo.

Baker, W. B. (B), Unaweld Mfg. Co. of Texas, 1503 Gentry, Houston, Texas.

Baker, William W. (C), 2320 Glenway Ave., Cincinnati, Ohio.

Bakshas, Sam P. (C), Box 303, Fort Peck, Mont.

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List of Members

(Nov. 15, 1937)

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Bagwell, M. W.
Begley, F. P.
Beldon, Arthur J.
Blesi, John E.
Braun, E. W.
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Harwell, T. W.
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Sanderson, D. E.
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Thompson, J. B.
Wessman, Arnold
Williamson, Boyce
Wright, Paul

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Woods, W. R.

East Lake—
Woods, W. P.

Fairfield, Ala.—
Clark, A. R.

Gadsden—
Korner, C. E.

Mobile—
Griser, J. M.

ARIZONA

Yuma—
McAlister, Eugen

ARKANSAS

Siloam Springs—
Stricklen, Eldred E.

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Lucas, John W.
Neiman, H. J.
Newby, H. L.
Park, H. W.
Radcliffe, Thos. D.

Bakersfield—
Blackman, Edwin N.

Bellflower—
Ireland, B. N.

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Ward, N. F.

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Sturgess, Robert K.

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McCroskey, R. K.

Downey—
Schexschnider, Lee

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Jepsen, M. M.

El Segundo—
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Saunders, H. W.

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Venneman, Paul K.

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McHugh, J.

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Weeks, Lloyd W.

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Braun, M. H.

Lomita—
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Whittlesey, Harold C.
Williams, E. O.

Lynwood—
Nelson, Fred

Madera—
Emmert, Keith

Maywood—
Dana, Theron G.

Monrovia—
McMullen, L. A.

Montebello—
Cunningham, C. E.

Montery Park—
De Forest, Taber

Newport Beach—
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Hiner, W. E.
Shaw, C. M.

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Harcourt, Robert H.

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Archer, W. E.
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Cutler, Ralph W.
Taber, Clifford W.

Redondo Beach—
Alexander, A. J.

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Rio Vista—
Nelson, Julius
Whiteman, Paul

Riverside—
Brown, R. Earl

Roseville—
Collins, Frank E.

Sacramento—
Rose, Lambert V.

Salinas—
Curtis, Wm. F.

San Diego—
Goode, V. M.
Thornton, W. N.

San Fernando—
Pehoushek, F.

San Francisco—
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Bennett, James C.
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Low, E. C.
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Sorensen, Martin C.

Santa Maria—
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Santa Monica—
Coghlan, S. F.
Hoberg, Carl G.
Millar, Wm.
Reno, R. W.

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Southgate—
Haynes, A. R.

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FLORIDA

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Ruzich, Joseph S.
Samuelson, Geo. W.
Schipplock, C. J.
Seabloom, E. R.
Simpson, Lowell
Sir, W. W.
Skog, L.
Smith, Abram E.
Smith, A. E.
Smith, W. W.
Soden, Gene
Stanley, A.
Steinbrecher, A. H.
Strope, L. M.
Sullivan, J. F., Jr.
Taylor, J. Hall
Tenney, G. E.
Tholen, M. A.
Thomas, T. G.
Tolan, Richard L.
Tremelling, R. F.
Tumas, Martin
Turner, Clyde
Turner, C. A.
Unger, A. M.
VanNess, Russell A.
Vock, Louis G.
Weist, M. M.
White, Dan
Wilson, F. D.
Wolfe, Jonathan
Young, C. D.
Ziegler, H. F.

Dahinda—
Thurman, Orlo Z.

Decatur—
Cline, Earl

East Alton—
Cunningham, G.

East St. Louis—
Christie, Robert L.
Watson, R. D.

Edwardsville—
Sasek, Fred

Evanston—
Dowson, Harry
Mott, Chester

Glen Ellyn—
Gordon, Charles W.

Harvey—
Anderson, P. J.
Conant, E. D.
Craig, G.
Davis, G. F.
Dawson, J. Vernon
Hatke, H.
Jones, Wm.
Martin, L.
Ludtke, A.
McCune, Ed.
McPhee, L. S.
Moxie, L. C.
Parish, W. A.
Rundquist, Lester H.
Snyder, Carl
Storz, M.
Vickers, C.
Walls, C.
Wright, J. P.

Hazel Crest—
Comerford, J. E.
Frey, R.
Munro, Wm.

Homewood—
Toedte, T. E.

Joliet—
Dinoffria, L.
Perona, Louis A.

Kewanee—
Shinkevick, John

La Grange—
Blomberg, M.
Druetzler, Chas.
Heseltine, A.
Hruska, J.
Kuehn, Ernest
Peterson, A. V.
Willot, J.

Lombard—
Swift, Alden W.

Manhattan—
Dyer, Roy E.

Maywood—
Votruba, Frank

Melrose Park—
Stuckey, W. A.

Midlothian—
Van Iten, M.

Moline—
Burgston, C. H.
Kelsey, H. C.

Mt. Vernon—
Guyer, J. A.

Oak Park—
Lacey, Jim

Peoria—
Miskoe, W. I.

Princeville—
Wheatley, C. E.

Riverside—
Habel, B. R.

Rockford—
Andrews, Chas.
Campbell, Chas. A.

Roseland—
Dunnett, D.
Murphy, C.
Seyffert, E.

St. Clair County—
Greenspon, A.

Thornton—
Diekelman, W.

Tinley Park—
Chapman, E.

Urbana—
Williams, Bart
Williams, Robert D.
Wilson, Wilbur M.

Wilmette—
Tarbell, R. P.

Winnetka—
Plumley, Stuart

Wood River—
Cummings, R. L.
Forcade, H. D.

Zion—
Moon, Arthur W.

INDIANA

Angola—
Francis, Harold Eugene

Cedar Lake—
Lee, Earl S.

Chesterton—
Anderson, H.

Dunkirk—
Brotherton, Dohrman C.

E. Chicago—
Cox, Chas. N.
Crawford, W. F.
Joyce, Edward, M.
Wang, Ray H.

Fort Wayne—
McClure, James M.

Hammond—
McKinzie, Daniel J.
Walcher, E.

***Indianapolis—**
Becherer, R. C.
Frank, Fred
Maxwell, W. H.
Notvest, Robert
Weiger, Joseph A.

Michigan City—
Logmann, A. A.

Mishawaka—
Schmidt, Geo. W.

Muncie—
Crapo, Fred M.
Houch, Ernest

Peru—
Volpert, W. J.

South Bend—
Fleming, Herbert J.
Innis, F. L.

IOWA

Bittendorf—
Carle, W. J.

Cedar Rapids—
Choate, R. E.
Malek, Henry

Des Moines—
Boian, W. O.
Miller, A. S.

Marshalltown—
Woollard, Harry

Mason City—
Barclay, Paul V.

KANSAS

Great Bend—
Kramer, Wayne F.

Greeley—
Bilderback Doyle R.
Bilderback, Emmet

Hiawatha—
Olsen, Bennett B.

Humbolt—
Lieurance, Delbert R.
Spencer, Coleman J.

Iola—
Berning, Harold J.
Lackey, Leo J.
Lackey, Wilbert B.

Kansas City—
Brackins, V. E.
Edwards, J. P.
Hall, J. A.
McDonald, V. G.
Mika, H. L.
Ward, H. E.

Lawrence—
Detlor, Leonard T.
Sluss, A. H.

Leavenworth—
Jones, C. S.

Osage City—
Steinhoff, Wesley

Ottawa—
Garrison, Paul R.

Paola—
Dowell, Fred H.
Hunt, Crawford L.
Jewett, Francis R.
White, Merle W.

Stockton—
Hus, S. F.

Topeka—
Grover, LaMotte

Wichita—
Atherton, A. W.
Brown, G. G.
Easton, W. J.
Green, J. R.
Kopper, H. D.
Lackey, Walter
McFaddin, M. D.
Sweeney, E. J.
Townsend, R. L.
Worden, Robert L.

KENTUCKY

Ashland—
Cunningham, J. B.

Ft. Thomas—
Long, J. M.

Louisville—
Clark, Wm. R.
Fritsch, R. E.
Hicks, Gus H.
Lewis, Theodore
Martin, S. B.
Plinke, William

LOUISIANA

Baton Rouge—
Elbourne, C. H.
Smith, Everett G.

New Orleans—
Barrios, E. G.
Clothier, A. L.
Guillot, A. H.

Port Sulphur—
McLaughlan, O. B.

MARYLAND

Annapolis—
Amoss, John K.
Ronay, Bela M.

Baltimore—
Arness, W. B.
Ballantyne, W. M. H.
Ballard, Wilson T.
Barkow, Armano G. L.
Boetcher, Hans Nielsen
Burggrof, Fred
Canty, T. A.
Carper, E. R.
Christensen, L. H.
Courtney, James A.
Cromer, A. B.
Cumberland, John C.
Dorsey, J. Richard
Foster, Albert W.
Foster, Edwin K.
Garcia, J. F.
Halas, John
Higgins, N. B.
Hilbinger, C. N.
Jenkins, Alex F.
Johnson, James A.
Kay, G. L.
Knight, A. Rhodes
Lane, J. Melvin
Lang, P. G., Jr.
Mansfield, Roy A.
Michel, C. H.
Ortman, Edw. F.
Patterson, Wm.
Phillips, Chas. J.
Saxe, Van Rensselaer P.
Thompson, Arthur C.
Steele, M. G.
Wicker, M. G.
Zouck, George H.

Brentwood—
Hetherington, Chas. R.

Cambridge—
Jones, A. D.

Catonsville—
Gathmann, Emil

Chevy Chase—
Wilson, James W.

Dundalk—
Miller, Dr. John W.

Easton—
Taylor, W. L.

Essex P. O.—
James, Elwood F.

Frederick—
Rosenbaum, P. D.

Hagerstown—
Brimmer, Fred J.
Wingert, Byron E.

Sparrows Point—
Birkholz, John
Gould, Leo J.
Johns, L. F.
Ross, Harry H.

Towson—
Adams, C. H.
Taylor, James E.

MASSACHUSETTS

Adams—
Powers, J. F.

Athol—
Thayer, Frank L.

Beverly—
Horgan, Patrick J.

Boston—
Austin, Herbert G.
Barnes, Wm. H.
Canty, J. P.
Combs, A. L.
Dahlgren, R. F.
De Blois, Weldin
Delbridge, C.
Doherty, E. R.
Faden, James L.
Harris, H. H.
Idell, Percy C.
Jackson, L. F.
Kenney, A. A.
Lockman, Edward L.
MacDermod, Elliott
Mehaffey, Frank B.
Mingotte, Eugene V.
Norwood, Edgar A.
Peabody, Harold P.
Raney, J. E.
Rugg, Philip N.
Slater, S. J.
Steward, Harry M.
Stolba, E. F.
Strathdee, Wallace B.
Sweet, H. A.
Walker, Frank B.
Whitney, J. T.

Brighton—
Chase, Philip T.

Brookline—
Le Bel, Everett M.
Morgan, J.

Cambridge—
Bullock, H. R.
Ewertz, Harold N.
Feyling, P. L. F.
Gordon, Leo
Haertlein, Albert
Jefferson, John A.
Miller, David
Moody, Chas. G.
Westendarp, Carl F.
Wilson, Francis W.

Charlestown—
Donovan, James

Everett—
Bengston, N. B.

Fitchburg—
Birnie, A. W.

Hingham—
Ovaska, A. Walter

Holyoke—
Marran, Vincent P.

Hopedale—
Taft, Harold G.

Jamaica Plain—
Giduz, Fred J.

Lynn—
Davis, Fred
Hill, Kenneth P.
Knight, Frederick T.
Richardson, George
Speed, Francis H.
Thomson, Malcolm

Maynard—
Shaw, Earston L.

Natick—
Lovejoy, Ralph F.

Peabody—
Fitzmaurice, John A.

Pittsfield—
Borges, Warren
Calder, W. A.
Coakley, Joseph
Culverhouse, K. J.
Doolan, Michael C.
Fahey, William Edward
Fallon, F. D.
Gilbert, Ralph A.
Holt, E. S.
Hopley, David
Jackson, L. E.
Kehn, Louis C.
Lawlor, Patrick J.
Lipschitz, William L.
Meyers, Edwin J.
Potter, Everett F.
Pucko, F. B.
Seymour, W. A.
Somerville, G. G.
Unger, M.
Weldon, J. J.

Quincy—
Debes, E. D.
Field, Paul
Hogaboom, Allen G.
Rober, Chester A.
Smith, John

Randolph—
Wescott, George A.

Sharon—
Mower, D. A.

Somerville—
Lundgren, James E.

Southbridge—
Damian, Stephen J.

Springfield—
Walsh, B. M.

Waltham—
Hoffman, C. R.

Watertown—
Warner, Wm. L.

Westborough—
Johnson, Carl C.

West Medford—
Coombs, Anthony S.

Williamstown—
Beverly, Chas. A.

Wollaston—
Peterson, W. L.
Spear, Joseph, Jr.

Worcester—
Coster, Charles H. E.
Elliott, Edward
Hall, P. W.
McKee, W. J.

MICHIGAN

Allan Park—
Zorn, William H.

Ann Arbor—
Spindler, Wm. A.
Teifer, Wm.
Volz, Albert C.

Bay City—
Herbert, John W.
Nichols, Leonard E.

Benton Arbor—
Dunn, U. S.
Maine, William D.

Dearborn—
Foss, E. H.

Detroit—
Allen, Wm. G.
Anderson, Walter
Bailey, Robert P.
Bramlett, James B.
Brueckner, Julius

*Section organized November 18th. New members will be included in the January issue.

Calley, Joseph
Capper, W. M.
Centers, C.
Corey, Donald H.
Crigger, John
David, Wm. T., Jr.
De Wid, William
Dockson, C. H.
Doud, H. P.
Douglass, A. S.
Eksergian, C. L.
Ellis, Clayton C.
Ellis, Harold J.
Fassler, P. W.
Flynn, C. F.
Friebel, G. J.
Goodspeed, Elvin S.
Goude, Charles
Harvey, S. M.
Hayes, Wm. M.
Heckman, Homer H.
Hill, L. Clayton
Holtzhouse, J. A.
Honhart, Jack C.
Jackson, H. A.
Jeffery, E. P.
Kenniston, F. R.
Lane, Arthur L.
Lewis, Frank
Maichle, F. M.
Matte, Joseph, Jr.
Peattie, William W.
Phelps, G. E.
Phillips, C. E.
Plant, A. F.
Reid, Vaughan
Robertson, Earl
Robinson, James W.
Roth, Henry W.
Rousseau, E. J.
Sadler, Leon H.
Shelton, E. P.
Sieger, G. N.
Simmons, Walter H.
Smith, Gilbert C.
Smith, O. R.
Sonneborn, Arthur B.
Sparkman, Hall Thaddens
Speake, Albert C.
Stasz, John
Steel, Beaumont A.
Steel, Thos. A.
Stone, E. C.
Sudlow, Edward
Takasy, Louis
Tebben, John D.
Thomas, James F.
Vrooman, G. C.
Waldschmidt, E. K.
Weihe, Cal.
Welcomb, C. D.
White, William J.
Williams, Howard L.
Young, George C.

Ferndale—
Benkert, Louis M.

Flint—
Braik, Ralph
Jedlick, Charles L.
Powless, L. L.
Spice, Stuart N.

Grand Rapids—
Layman, R. D.

Houghton—
Young, A. P.

Jackson—
Dill, Paul I.
Dissmeyer, E. F.

Lansing—
Goff, R.
Puffer, Harold R.

Melvindale—
Sevcik, John

Ontonagon—
Beck, John
Wolf, Joseph

River Rouge—
Brendle, Russell W.
Bronson, Charles
Drovillard, Elmer J.
Foote, Frank E.
Gilbert, John
Nadeau, Edward T.
Stewart, C.

Royal Oak—
Allen, Guy F.
Cramb, D. L.
Ott, Burl E.
Perkins, Charles L.

Saginaw—
Barnes, D. H.

Sault Ste. Marie—
La Plaunt, Wilvy J.

Three Rivers
Anderson, F. A.

Wyandotte
Robinson, Jack
Robinson, Maxwell H.

Ypsilanti—
Luebke, James L.

MINNESOTA

Albert Lea
Koch, Arthur

Duluth—
Mars, R. S.

Minneapolis—
Akins, C. M.
Brown, Walter V.
Canfield, Norwood N.
Chatfield, Howard
Cook, E. A.
Fowler, Bert
Graneman, F. W.
Hall, Edgar E.
Hoglund, Clarence E.
Hudoba, Louis
Irwin, G. W.
Johnson, Amos
Johnson, E. R.
Jornlin, K.
Klass, Fred
Kruse, Peter R.
Louis, Harry
Mansfield, Robert
McBurney, L. L.
McLean, D. G.
Memorich, John
Moore, O. F.
Murphy, Wm. E.
Nelson, John
Newton, R. H.
Nordgren, Carl O.
Olson, Jacob
Potter, Morgan H.
Ramage, Walter D.
Riebeth, C. E.
Robinson, R. W.
Ross, Fred
Schmidt, Mike
Simonsen, E.
Warmington, Thos. J.
Watson, G. V.
Williams, J. E.
Wilson, Chas. R.

Nashwauk—
Tuomala, J. Evald

St. Paul—
Barron, J. H.
Bodin, C. E.
Bollenbach, W. M.
Comfort, C. E.
Getsug, Bert
Hill, Gladstone C.
Kicherer, H. J.
Kilbane, James A., Jr.
Lindeke, H. A.
Nielsen, Howard
Nystrom, Carl
Schroeder, Emil C.
Schulz, William H.
Swanson, George

Walker—
Lorge, Willard F.

MISSISSIPPI

Jackson—
Ellis, E. B.

MISSOURI

Carrollton
Allison, Edgar T.
Carewe, Patrick J.
Filbin, Clement J.
Lutz, Elmer C.
Shirley, Hugh K.

Englewood—
Cox, Leslie K.

Fulton—
Fairchild, Frank

Gorin—
Lewis, Harley S.
Lewis, Raymond
Marshall, Walter

Grandview—
Duncan, Spurgen A.
Hosler, Murl C.
Knapheide, Ernest W.

Hardin—
Transue, S. W.

Hickman Mills—
Williams, Leslie N.

Higbee—
Lewis, Earl

Independence—
Fagan, Henry F.
Gore, Burney F.
Goulden, R. W.
Short, E.

Jefferson City—
Kelling, Scott
Sack, N. R.
Sinn, Joseph

Kansas City—
Adolphsen, A. W.
Amoneno, Ernest
Anderson, E. D.
Athern, W.
Blazer, Earl H.
Burge, R. G.
Camp, C. L.
Carter, G. C.
Clark, W. R., Jr.
Cramer, F. E.
Cramer, L. L.
Culver, J. M.
DeMuth, I. C.
Dickins, G. F.
Donegan, Charles E.
Frick, C. W.
Gill, Ed. H.
Haggard, H. W.
Hartung, Homer C.
Helman, Carl H.
Herrick, Clayton B.
Hitchcock, H. R.
Hohenschild, W. J.
Hunt, V. H.
Kirk, J. P.
Koons, R. E.
Kunert, Max J.
Kurtz, W. H.
Lighfoot, C. R.
Majewski, Paul M.
Mauck, Orville
Meyers, A. M.
Mika, C. T.
Miller, J. R.
Powell, C.
Roberts, J.
Roth, A. W.
Sayner, R. O.
Strauss, George J.
Swope, L. H.
Voigtlander, W.
Warren, Okie L.
White, R. B.
Wilson, Norman A.
Woodman, C. E.

La Plata—
Benjamin, C. E.
Benjamin, John
Gillespie, James E.
Hatcher, Ernest W.
Henderson, H. L.

Louisiana—
Turner, Edward G.

Marceline—
Flickenger, J. R.

Martin City—
Hendrix, Jewell G.

Norborne—
Wilson, Ogle H.

Richmond Heights—
Adank, J. L.

St. Josephs—
Hamlet, L. M.
Michels, H. A.

St. Louis—
Adams, M. P.
Alina, A.
Baker, T. R.
Bleikamp, C. E.
Brumbaugh, W.
Close, H. C.
Corby, W. E.
Cullinane, G. M.
Dumser, J. M.
Emory, John B.
Enslin, E. M.
Fantz, F. C.
Fantz, Fred D.
Feldhaus, F. J.
Feldhaus, Richard J.
Garner, F. R.
Gehinger, R. F.
Griesel, George
Hepper, Richard A.
Hill, Lockwood
Hoffman, Lester O.
Howery, Theodore E.
Jacskon, Earl T.
Johnson, E. W.
Joslin, E. W.
Kellogg, W. P.
Kohlbry, F. P.
Kroeger, Arthur
Leonard, B. H.
Lohbiller, H. J.
Merkt, Joseph
Meyerson, M. E.
Moss, N. F.
Netchvolodoff, V. V.
Olivo, Albert R.
Pahmeyer, Fred O.
Plane, John C.
Pyle, G. I.
Radford, Fred
Reeves, E. L.
Rosborough, J. G., Jr.
Roy, E. A.
Royer, J. W.
Sammelman, C. W. S.
Schenler, Walter M.
Schwarz, Arthur S.
Scioletich, M. B.
Shipley, T. G.
Sheaff, H. H.
Sloan, Chas. N.
Stupp, Norman J.
Thomas, R. H.
Thumser, R. C.
Todd, Floyd
Todd, Marion R.
Toth, Stephan C.
Webb, E.
Wehmeyer, A.
Wilkinson, O. F.

University City—
Clements, D. A.

Webster Groves—
Hicks, H. E.

MONTANA

Billings—
Reay, Lee
Wolfe, H. W.

Bozeman—
Hitchcock, Orvin
Homann, F. C.

Fort Peck—
Anderson, Wm. C.
Bakshas, Sam P.
Bates, Allan W.
Bostrack, Ole
Capper, Chester W.
Cardani, Peter J.
Charles, John C.
Conti, Joseph F.
Fink, Kenneth E.
Garmon, Charles F.
Jefferson, T. B.
Liggett, Donald
McCarty, Wayne E.
Miranda, Elwood C.
Pelland, Bert T.
Peterson, Chester A.
Rumfelt, Henry F. C.
Seals, Elmer E.
Spaulding, Ray L.
Sorensen, Julius T.
Sturm, Leland A.
Wedgeworth, James F.
Wilkinson, James R.

Gallatin Gateway—
Nixon, Delbert R.

Gardiner—
Lamach, John

Glasgow—
Lewis, W. A.

Helena—
Hare, L. F.

Lewistown—
Anderson, James E.

McCone City—
Burger, Curtis C.

Nashua—
Chan, Adolf F.
Galloway, W. M.
Trimble, Lawrence B.

New Deal—
Gray, W. P.

Polytechnic—
Martin, Charles Edward

Wheeler—
Griffith, Harry S.

NEBRASKA

Alliance—
Feagins, A. E.

Louisville—
Wright, P. L.

Omaha—
Bradway, I. C.
Engler, O. E.
Fuchs, B. L.
Johnson, A. B.
Knox, Charles
McGrath, C. E.
McShane, T. S.
Mucha, Kamil
Mullane, Clinton W.
Palmer, J. T.
Schneiderwind, L. O.
Shiner, G. E.
Smith, Arthur
Smith, W. G.
Thomas, Lester B.
Thrapp, George A.
Vollmer, Henry G.
Wickman, J. C.
Williams, Arthur W.
Wills, George E.

NEVADA

Boulder City—
Carrier, Louis E.

Ruth—
Blackmore, F. L.

NEW HAMPSHIRE

Atkinson—
Strogney, S. A.

Claremont—
Ericson, Carl B.

Portsmouth—
Irons, J. D.

NEW JERSEY

Bayonne—
Frohlin, J.
Hughes, W. D.
Laurinatis, J.
Male, Seymour
Massa, Wm. J.
Robertson, R. E.
Schoener, G. F.

Bloomfield—
Fenwick, Fred G.
Obrieter, Robert B.
Young, C. R.

Camden—
Frey, Chas. A.
Griffin, Spencer A.
Pierce, H. W.

Carney's Point—
Rudinoff, Jack

Cranford—
Ogilvie, Douglas C.

East Orange—
Deming, G. M.
Grow, H. J.
Waring, Robert

Elizabeth—
Bauer, L. A.
Elly, Robt. D.
Fyke, F. C.
Holder, G. C
Loane, P. M.
Sheeley, M. F.

Englewood—
Koester, Geo. L., Jr.

Garwood—
Robinson, Walton S.

Grantwood—
White, Bernard L.

Harrison—
Moore, L. M.

Hasbrouck Heights—
Schnetzer, S.

Highbridge—
Hulbert, Wm. G.

Hillside—
Humm, E. F.

Hoboken—
Vogel, John L.

Irvington—
McPeek, Reynold

Jersey City—
Anderson, James L.
Begtrup, E. F.
Crowe, J. J.
Fisher, O. W.
Hopkins, R. K.
Kidd, Alexander
La France, B. A.
Levin, Nathan
Morris, S. G.
Neubauer, Paul
Rippel, J. N.
Shields, W.
Smith, T. B.
Van Riper, V. R.
Weinert, G.

Kearny—
Aborn, R. H.
Ohlssen, W. C.
Powell, R. E.
Schaffter, G. A.

Linden—
Andrews, F. H.

Maple Shade—
Doering, Walter

Merchantville—
Simpson, J. J.

Metuchen—
Hampton, D. H.
Hiecke, W. K.
Munz, H. J.
Punsalan, Leon F.
Tunner, Alex.

Millburn—
Sidney, C. Watson

Montclair—
Wilson, R. M.

Newark—
Britland, J. G.
Gunning, J.
Hollag, C. J.
Mattern, P. M.
Miller, R. O.
Payne, Burt H.
Schlitz T. R.
Swankhous, F. B.
Thomson, Alexander
Zimmerman, J. H.

New Brunswick—
Troger, H. H., Jr.

North Bergen—
Aschenbrenner, F. J.
Heller, J. M.
Scherrer, A. G.
Tisza, E. E.

Paterson—
Ennis, H. V.

Penns Grove—
Lumadue, C. H.

Perth Amboy—
Sprague, A. G.

Plainfield—
Halliburton, M. R.
Phillips, J. T.

Roselle Park—
Harvey, A. H.

Swedesboro—
Shaw, Franklin B.

Teaneck—
Forgett, V.
Rossheim, David B.
Stewart, S. S.

Trenton—
Harvey, Prof. W. E.

Union—
Lum, S. Clark

Union City—
Trench, William H.

Weehawken—
Blickman, Saul

Westfield—
Flocke, Frank G.
Grove, Wm. G.
Scharwenka, A. V.

Wharton—
Rusch, Charles E.

NEW YORK

Albany—
Browne, R. L.
Lipschitz, Harry

Amsterdam—
Sprong, Geo. A., Jr.

Astoria—
Randall, M.

Ballston Lake—
Houck, Arnold E.
Van Aernem, Ralph E.

Beechhurst—
Adelson, Samuel S.

Bellaire Park—
Klos, Clifford H.

Binghamton—
Jacob, Alfred G.

Buffalo—
Brand, E. A.
Carlin, L. C.
Carroll, M.
Cobb, G. W.
Freeman, Chas. S.
Gillespie, J. W.
Harnish, Alfred C.
Hebeler, Edw. S.
Howard, Frank O.
Korn, M. P.
Mason, J. H.
Meyer, A. R.
Mohring, R. F.
Parker, Geo. H.
Rodgers, F. L.
Sandberg, George H.
Schlieder, H. J.
Sequin, Joseph H.
Siemer, Robert
Wiesner, A. J.

Cattaraugus—
Mosher, Leland C.

Chittenango—
Lang, Philip A.
Pynn, J. Garland

Clarence Center—
Kjeldgaard, Andrew, Jr.

Cohoes—
Nelson, Harold
Waugh, J. E.

DePew—
Hutchinson, Jack
Hutchinson, T. A.

Dunkirk—
Gruenberg, J. W.

Elmhurst—
Goodman, R.

Endicott—
Rogers, Frank D.

Flushing—
Kelsey, Walter

Freeport—
Bieckerman, Harry

Fulton—
Haskell, Ben

Ithaca—
Ames, W. B., Jr.
Hollister, Prof. S. C.
Koshkin, Simeon J.
Thomas, R. David

Jackson Heights—
Rettig, Carl, Jr.
Rooney, J. I.

Kenmore—
Dingeldey, Carl C.
Dobler, H. F.
Koerner, Harry

Kingston—
Ashley, Charles

Lackawanna—
Walker, Kenneth L.

Little Falls—
Barnes, Harold N.

Livingston—
Boyd, D. E.

Long Island City—
Hallen, M.
Isenburger, H. R.
Kandel, Charles
Klevens, J. A.
Myhre, L. O.
Whaley, B. L.

Maspeth—
Rahtz, A. J.

Mount Vernon—
Coster, F. R.
Snyder, George V.

New Brighton—
Dimelow, Harry K.

New York City—
Abbott, Lester E.
Aitchison, R. B.
Arnott, David
Axline, R. A.
Axtell, A. C.
Bates, G. H.
Bedworth, Robert E.
Bellamy, Benj.
Booth, Henry
Borst, W. H.
Bremer, E. W.
Bryan, Chas. W., Jr.
Bulkley, Harold F.
B ll, G. N.
Burns, Wm. T.
Caldwell, Wm. M
Callahan, J. F.
Cameron, A., III.
Carpenter, A. W.
Carroll, W. C.
Carter, G. O.
Cassidy, P. R.
Cavanagh, R. F.
Chapin, Richard N.
Chase, C. N.
Clarkson, Stewart N.
Cohen, Fred W.
Connell, Fred
Critchett, J. H.
Dalcher, John T.
David, E. V.
Day, C. L.
De Golyer, A. G.
De Pew, T. Eugene
Deppeler, J. H.
Dierckx, Jules
Donald, R. S.
Doyle, E. A.
Drake, Harcourt C.
Edelson, Leo
Eder, F.
Edwards, James L.
Ewertz, E. H.
Farmer, F. M.
Febrey, H. H.
Fetherston, Thomas C.
Fish, Gilbert D.
Folowitz, A.
Frankland, F. H.
Fraser, O. B. J.
Gailor, C. F.
Gannett, J. K.
Gaynor, A. E.
Gibson, C. D. W.
Graham, R. R.
Greene, T. W.
Haag, J., Jr.
Hagstrom, T.
Haile, William M.
Hamer, T. M.
Hartstein, Fred W.
Hasler, T. B.
Hassett, James
Havens, Wm. W.
Hays, W. S.
Helmkamp, R. F.
Heymann, H.
Higbee, Clarence W.
Hoffman, E. H.
Holwill, Claude N.
Horton, Vincent C.
Hovey, O. E.
Hubach, Kenneth
Iwasaki, Massahide
Jacobus, D. S.
Johanson, Sven
Kazlavskas, A.
Kehl, R. L.
Keir, James H.
Kennedy, R. E.
Keogh, A. F.
Kiernan, N. F.
King, Frederick J.
Kinzel, A. B.
Kissock, A.
Kligora, H. J.
Kugler, A. N.
Lavine, G.
Lee, E. J.
Lindquist, Albert K.
Llewellyn, F. T.
Lobee, A. A.
Lowrie, W.
Maeurer, F. J.
Male, Milton
McCune, C. A.
McKay, R. J.
Meyer, C. G.
Milligan, R.
Mitchel, C.
Mitchell, Dr. W. M.
Moisseiff, Leon S.
Mooney, Ralph B.
Morgan, J. B.
Moss, Herbert H.
Naes, B.
Obert, C. W.
Oehler, Alfred G.
Oelhaf, C., Jr.
Oliver, Frank J.
Ostrander, F. T.
Palmer, Walter
Pearson, R. C.
Perry, C. B.
Pino, George B.
Place, G. W.
Place, J. W.
Priest, B. B.
Quasdorf, H. C.
Quintana, C. H. M.
Rechtin, E.
Reinhard, H. F.
Richards, Nathaniel A.
Roberts, D. E.
Robertson, G. C.
Rockefeller, H. E.
Rogers, C. E.
Rogers, F. E.
Rogers, H. L.
Roper, Edward H.
Rosencrans, J.
Rowland, W. B.
Russell, B. A.
Ryder, E. M. T.
Schermerhorn, J. A.
Schneider, George
Scott, C. H.
Seel e, E. E.
Sheffer, J. W.
Shimojo, T.
Slottman, G. V.
Smith, H. Sidney
Smith, Martin M.
Smith, William R.
Snowden, D. E.
Spielvogel, S. W.
Sporn, Philip
Steubing, A. F.
Stewart, R. W.
Sussman, H.
Swain, Philip W.
Sykes, George
Tangerman, E. J.
Teall, Charles H.
Thomson, D.
Tickle, A. B., Jr.
Tinnon, John B.
Townsend, J. R.
Ullmer, Herman
Urbach, Louis
Van Alstyne, G. T.
Vom Steeg, E., Jr.
Watson, Charles H.
Weigel, A. C.
Weymouth, Aubrey
Whitney, H. LeRoy
Wikoff, Alan G.
Yates, Ira B.
Zimmer, Rudolf

Niagara Falls—
Davol, O. H.
Dawson, J. R.
Fish, J. A.
Hettler, Wm. M.
Howell, W. H.
Huse, Harold M.
Kozar, Leon C.
Miller, W. B.
Misener, G. L.

Norwich—
Fancher, A. C.

Patchogue—
Alexander, Chas. E.

Perry—
Jarman, D. E.

Poughkeepsie—
Hart, F. H. M.

Richmond Hill—
Dickau, Henry H.

Rochester—
Dick, Walter G.
Gaylord, Thos. S.
Kazoroski, W. J.
Miller, Benjamin
Stelljes, Gomer

Rockville Center—
Boggs, Robt. W.

Rome—
Wilkins, Richard A.

St. Albans—
Scott, S. S.
Spraragen, W.

Schenectady—
Anderson, James C.
Aumiller, W. L.
Best, Robt. C.
Borkowski, John J.
Botkis, William J.
Braley, Loy S.
Buckholz, Gerard R.
Compriello, A.
Chapman, V. J.
Churchill, William W.
Clark, R. W.
Cochrane, A. G.
Cognetta, James J.
Deangelis, Louis A.
Dooney, Charles J.
Durham, Clyde
Edwards, Edward J.
Egry, Victor
Elwood, E. ., Jr.
Fagel, John E.
Fox, Harry W.
Gibbs, Louis T.
Gillette, Robert T.
Governiski, Anthony
Haughton, F. A.
Hicks, Jesse F.
Hobart, H. M.
Hutchins, W. C.
Jesperson, Chas.
Kehoe, Stephen J.
Kingsley, Geo. B.
Kortz, Howard R.
Krumal, Frank
La Badie, James V.
Leveen, L. R.
Lipsky, Joseph J.
McAuliffe, J. J.
McGrath, T. E.
McNeil, James Kenneth
Meeker, Lincoln D.
Miller, F. Harold
Moore, Thomas J.
Morrison, William H.
Near, A. E.
Nelson, S. D.
Nigriny, Victor
Owen, Neil W.
Palmer, Robert
Patterson, Harold B.
Pelton, R. S.
Ramunti, Nicholas
Rapant, Joe
Rash, William C.
Raymo, A. J.
Redmond, James H.
Ross, George Arthur
Sadlowski, Peter
Seely, Ralph W.
Sheldon, L. A.
Sherman, Walter P.
Spittler, E. R.
Stevens, A. C.
Taylor, Bruce M.
Tenes, Lawrence N.
Turner, R. F.
Tuttle, Frank B.
Vogel, Andrew
Wilke, R. H.
Willey, Raymond C.
Wilson, Wayne E.
Wood, A. P.
Wyer, R. F.
Yager, George L.

Schoharie—
Sellick, Edw.

Scotia—
Binck, Arthur S.
Bondley, R. J.
Burgenbauch, T. K.
Burnett, C. M., Jr.
Catlett, James T.
Festino, John
Gruman, Carl A.
Kuhlberg, Carl F.
Lair, W. B.
Miller, V.
Raine, Francis H.
Ransom, James M.
Ransom, Robert G.
Ratcliffe, Wm.
Rynning, Jerome L.
Sharadin, Kenneth R.
Smith, Sidney C.

Silver Lake—
Bernard, D. H.

Sunnyside—
Scharlach, C. D.

Syracuse—
Bentley, Harold P.
Brown, Jesse J.
Kogut, Joseph
Osborne, S. C.
Patterson, C. T.
Roscoe, J. S.

Tonawanda—
Jones, Walter N.

Troy—
Hess, W. F.
Lawson, T. R.
Stockholm, Seth R.

Tuckahoe—
Dayton, C. L.

University Heights—
Yasines, S. Franz

Watervliet—
De Vries, R. P.
Franklin, Raymond J.
Templeton, C. B.

Wellsville—
Palmer, M. A.

White Plains—
Edmondson, G. D.

Williamsville—
Schulenberg, E. A.

Wortendyke—
Broadrurst, Chas. T.

Yonkers—
Ehrlich, R. H.

NORTH CAROLINA

Charlotte—
Walsh, H. I.

Fayetteville—
Owen, F. C.
Whitted, L. N.

OHIO

Akron—
Johnston, H. Clark
Sherbondy, F. G.
Yanley, Frederick Wilson

Alliance—
La Salle, W. M.
Shem, G. W.

Avon Lake—
Jones, Harold O.

Barberton—
Hodge, J. C.

Batavia—
Didday, Herman C.

Beloit—
Stanley, Leroy F.

Cambridge—
Bates, Earl

Canton—
Hever, A. G.
Miller, W. Lloyd
Wilson, Ralph L.

Cincinnati—
Baker, William W.
Barker, Geo.
Blakeney, Lewis B.
Breese, Clayton D.
Casey, R. H.
Clark, Harry R.
Davis, Augustine
Dettwiler, W. P.
Gardner, D. G.
Heinzelman, Fred
King, Robert
Kraus, Rudolph
Maddox, W. A.
Naegele, W. R.
O'Hair, W. V.
O'Hara, Eric
Osborne, M. J.
Paolino, A. G.
Paolino, Laurence
Petry, Walter W.
Rosenblum, Abe
Ross, James K.
Stoudt, John M.
Thompson, James W.
Tilton, O. A.
Welling, Harry H.

Cleveland—
Adelson, J. S.
Ames, Ethan E.
Augustson, C. G.
Axtell, T. H.
Barnhart, C. Glenn
Bath, Cyril J.
Benedict, E. R.
Berkeley, Landon R.
Boom, W. B.
Boylston, Herbert M.
Brown, Alexander C., Jr.
Brown, Dana C.
Burr, Louis H.
Campbell, Lorn, Jr.
Carbis, J.
Champion, Pierre
Chonko, Mike
Collins, F. J.
Coon, Merritt G.
Cooper, W. K.
Davidson, Wm. H.
Davis, A. F.
Day, Charles A.
Driscoll, J. M.
Dunbar, S. J.
Dunham, L. H.
Dunn, Herbert
Dyar, H. H.
Dyer, Wm.
Eisenman, W. H.
Elder, Clayton T.
Engle, A. D.
Esslinger, Fred J.
Ferguson, J. E.
Fraser, P. V.
Frick, John
Gallitte, Anthony H.
Gast, R. A.
Gibson, Alfred E.
Golds worth, Harry E.
Hall, Wm. G.
Henn, Philip F.
Henriques, H. F.
Hexter, Irving B.
Hippe, Mark W.
Hopkins, J. C.
Hurley, Jack
Hurt, Roy J.
Jerabek, T. E.
Kavanaugh, Frank
Kells, T. Gilbert
Kimball, Don
Kincaid, John H.
Kinkead, Robt. E.
Kessler, F. W.
Kneen, H. F.
Knowles, A. M.
Lanken, C. C.
Lawless, T. J.
Lawrence, G. R.
Lichtinger, A. J.
Lincoln, J. F.
Lisy, John W.
Louis, Cornelius H.
Loveman, W. H.
Low, Albert S.
Lucas, Glen A.
Lucas, John J.
MacCorkle, E. W., Jr.
Madson, Arthur E.
Mathews, John M.
Maurath, George A.
McCreery, H. L.
McDonough, W. R.
McLoud, F. A.
Medsker, Charles A.
Mello, Pete
Michel, Wm.
Miter, Harry F.
Morrison, J. R.
Nichols, C. R.
Nutt, Buell W.
Oldenkamp, Henry
Orlando, Andrew
Peters, V.
Pfeil, A. Leslie
Plank, Fermer E.
Plummer, Fred L.
Plummer, Richard H.
Quist, Ralph
Rattle, H. Mansfield
Rice, Carl E.
Rice, William H.
Richards, J. S.
Riffie, Jacob F.
Sangdahl, Geo. S.
Schafer, Charles N.
Scheffler, C. A.
Scott, Edw. T.
Sessions, F. L.
Shaner, E. L.
Simera, Charles
Simon, Edward F.
Smith, E. A.
Smith, Earle C.
Smith, E. W. P.
Steinman, John E.
Stewart, W. S.
Taylor, C. M.
Taylor, Maurice R.
Thompson, L. R.
Thum, Ernest E.
Tinker, G. H.
Wagner, J. F.
Welker, Howard P.
Westendarp, H. O., Jr.
Williams, A. M.
Williams, Gordon T.
Willins, G. L.
Wolfenden, John T.
Wyss, J. Calvin

Columbus—
Black, J. N.
Egan, Howard P.

Cuyahoga Falls—
Fisher, Robert

Dayton—
Carl, James W.
Glenn, E. M.
Mecham, F. L.
Pederson, E. B.
Sauer, Carl M.
Toulmin, Harry Aubrey, Jr.
Weiler, Herbert G.
Wolfe, Clarence E.

Dennison—
McGuire, W. B.

Duncan Falls—
Farris, M. A.

Geneva—
Cowdery, Robert A.
Wright, Carl

Hamilton—
Moorhead, Russell H.

Kent—
Boucher, A. F.

Kinsman—
McBerty, F. P.

Lakewood—
Boehmke, Arthur H.
MacPherson, Kenneth
Pearse, B. R.
Williams, Robert N.
Wolcott, J. E., Jr.

Lebanon—
Hittle, C. H.

Lima—
Huston, William M.

Lorain—
Findlay, Samuel N.
McCormick, W. B.
Ziegler, L. C.

Mansfield—
Crawford, H. D.
Frank, M. M.
Mackey, Glenn C.
McDermid, J. A.

Marietta—
Lindemore, C. O.

Marion—
Rogers, H. L.

Massillon—
Henshaw, Richard A.
Lichtenwalter, T. R.
Love, W. L.
Miller, H. L.
Osborne, William S.
Powell, D. C.
Wallace, Robt. M.

Middletown—
Barnard, R. E.
Hayes, Anson
Kenney, Louis T.
Petry, Lewis W.

Montpelier—
Anspaugh, Eldon

Mount Vernon—
Taylor, Albert A.

North Canton—
Helder, L. G.

Painesville—
Hilberg, F. E.

Parma Heights—
Matusek, Otto

Rocky River—
Powers, Edward C.

Salem—
Gilson, William E.

Sandusky—
Thrasher, C. A., Jr.

Shaker Heights—
Blum, Harold P.
Munson, George

South Akron—
Smitzer, De Vere

South Euclid—
Landis, George G.

Steubenville—
Moxley, T. R.

Toledo—
Frautschi, Carl
Hansen, Hans
Homrighaus, A. H.

Trenton—
Miller, Harry C.

Troy—
Chaffee, W. J.
Hobart, W. H.
Thompson, Leslie

University Heights—
Dewald, W. A.

Warren—
Brague, William W.
Buecken, Hans E.
Byers, C. H.
Cella, Peter, J.
Cooper, J. H.
Corean, John T.
Delvecchio, E. J.
Haas, Robt. E.
Jones, James
Lasher, E. R.
Mackenzie, F. W.
Mallett, E. A.
Porter, F. N.
Roberts, N. J.
Sinclair, Richard G.
Vick, A. C.
Woofter, H. A.

West Middletown—
Taylor, A. E.

Wickliffe—
Wehr, Wm. G.

Wyoming—
Smith, F. H.

Youngstown—
Donahue, C. L.
Hughes, Grover A.
Lamond, Nestor T.
Moriarty, John J.
Reinhardt, G. A.
Smith, Henry H.
Sweesy, Floyd D.
Tolman, Lee Prescott
Tross, Ernest
Wills, Christian A.

OKLAHOMA

Bartesville—
Kindsvater, E. F.
Price, Harold C.

Muskogee—
McManus, S. M.

Oklahoma City—
Acker, L. E.
Banks, K. B.
Barnett, O. T.
Beathy, B. V.
Bendit, L. C.
Bowen, Charles
Brown, C. M.
Bullock, C. L.
Crane, V. B.
Day, Geo.
Dunne, M. F.
Ewing, H. E.
Farris, Ted
Fitch, Russell R.
Fulton, O. B.
Grundish, J. P.
Hanna, E. W.
Hefner, R. C.
Jolliffe, V. A.
Klein, Wm.
Lang, J. L.
Marriott, Richard G.
Mead, Denver
Mideke, Jos. M.
Mossholder, M. G.
Newhall, W. H.
Phillips, R. L.
Porter, J. S.
Raymond, G.
Roach, Justin
Robson, L. D.
Sprehe, Francis
Turner, J. D.
Underwood, B. O.
Vrooman, A. J.
Walcott, Ben E.
Whaley, Wm.
Willis, C. C.
Winneberger, J. H.

Seminola—
Copeland, R. D.
Rogers, M. L.

Stillwater—
Soderstrom, E. D.

Tulsa—
Hunt, S. D.
Leach, D. A.
Moorer, Wm. D.
Patterson, N. R.
Walker, Jay P.

W. Tulsa—
Wiley, G. L.

OREGON

Brandon—
York, Ralph L.

Grants Pass—
Hershberger, Adrian

Portland—
Dean, Carroll W.
Kordic, J. F.
La Velle, Eugene B.
Marsh, Fred H.
Place, L. H.

The Dalles—
Montgomery, L. S.

PENNSYLVANIA

Allentown—
Baines, Harry C.
Grebe, Karl A.
Kenworthy, J. W.
McGonigle, J. L.

Alquippa—
Van Orner, L.

Ambler—
Bartges, W. S.
Fairchild, G. Barnett

Ambridge—
Meyer, William

Ardmore—
Benker, C. H.
Sivyer, William

Bellevue—
Crouch, R. G.
Stulen, F. B.

Ben Avon—
Loos, C. E.
Wisner, J. L.

Berwick—
Evans, M. S.
Steinmeyer, J. W.

Bethlehem—
Bowles, H. J.
Durkee, E. L.
Hill, H. O.
Hilpert, M. G.
Jensen, Cyril D.
Jones, Jonathan
Lawson, H. W.
Lewis, Robert B.
Lewis, Wm. W.
Lyse, Inge
Rea, George H.
Schenck, Charles

Blairsville—
Andre, Frank V.

Brackenridge—
Patterson, Robt., Jr.

Bridgeport—
Greth, Joseph

Bridgeville—
Rinek, J. O.
Strauss, Jerome

Bristol—
De Ganahl, Carl

Butler—
Helsel, W. G.

Camp Hill—
Keyser, Cares C.

Cheltenham—
Schmidt, Herbert

Chester—
Bowers, F. M.
Butler, Harry
Hays, Athel V.
Holzbaur, Arthur A.
Jackson, T. M.
Lloyd, S. Elvin
Shaver, P. E.
Wagner, Albert H.

Coatesville—
Chapman, Everett
Griffith, Walter M.
Theisinger, W. Gordon.

Conshohocken—
Marland, Arthur.

Coplay—
Fisher, Robt.

Cornwells Heights—
Vansant, Elmer B.

Downington—
Bert, John D.
Johnson, Ashmore C.
Nick, Robert

Easton—
Smart, Walter C. H.

Erie—
Boyd, Wilfred E.
Burke, James
Humble, C. E.
Tsagaris, John G.

Folcroft—
Gaul, Rufus H.

Forty Fort—
Sutter, John

Fredonia—
Love, P. O.
Reinhart, H. D.

Glenside—
Ostrom, Kurre W.

Greensburg—
Booth, T. H.
Osmin, Basil N.

Greenville—
Allen, J. R.
Armour, L. E.
Bartlett, C. B.
Beil, T. K.
Bubeck, Lawrence
Burt, F. S.
Burt, R. F.
Clark, L. A.
Conlin, James
Dombacher, Jack
Garts, C. W.
Groover, K. L.
Howard, P.
Lewis, Charles
McClimans, F. A.
McCright, P. H.
McGill, C. E.
Neighbour, A. C.
Nesbitt, W. S.
Rohrer, H. B.
Seth, R. E.
Sowash, P. H., Jr.
Tam, W.
Weaver, N. J.

Grove City—
Buchanan, Wirt F.

Harrisville—
Hoffman, J. E.

Homestead—
Edgecomb, Fred J.

Jeannette—
Sturgeon, W. C.

Jenkintown—
Flower, Arch T.

Jersey Shore—
Kaukeinen, R. M.

Johnstown—
Alexander, G.
McCormick, John A.
Noderer, R. H.
Steward, Douglas P.

Lancaster—
Posey, M. W.

Lansdale—
Brettschneider, F. J.
Schaible, Frank W.

Lansdowne—
Mathews, S. B.

Linwood—
Davis, William J.

Lock Haven—
Claster, A. H.

Marcus Hook—
Bagsar, Dr. A. B.
Carney, J. E.
Jackson, Chas.
Joyce, E.
Pietsch, W. H.
Soden, E.
Tunis, Richard

Midland—
Eaton, Aaron L.

Milton—
Anchor, Chas. J.

Monaca—
Feely, H. M., Jr.

Monessen—
Bleecker, W. H.
Flaherty, John J.
Mechem, R. H.

Muncy—
Green, M. C.

Nazareth—
Edelman, Howard J.

New Castle—
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***Bethlehem Steel Company, General Offices, Bethlehem, Pa.,** produces steel in all grades and analyses, and manufactures a wide range of commercial steel products such as: Alloy Steels; Bars and Special Sections; Boiler Tubes; Bolts, Nuts, Rivets, Spikes; Bridges, Buildings and Steel Structures; Building Specialties; Steel Plate Work and Construction Castings; Corrosion-Resisting Steels; Flanged Products; Forgings; Pig Iron; Rails and Accessories; Semi-Finished Steel; Freight and Passenger Cars, Mine Cars; Steel Joists and Studs; Pipe; Plates; Sheets; Sheet, H and Z-Piling; Structural Shapes; Tin Plate; Tools and Tool Steels; Trackwork; Wheels, Axles; Wire, Nails, Fence, etc.; Wire Rope and Strand.

***The Big Three Welding Equipment Company** with offices and warehouses at Houston, Fort Worth and Corpus Christi, Texas, Tulsa, Oklahoma City, Oklahoma, are selling and servicing Lincoln Electric Company's welding machines, all types, such as engine driven, electric driven, belted units, steam turbine and Diesel driven welding machines, welding cables, helmets, face shields, lenses, holders and parts. Fleetweld electrodes, stainless steel, chrome, hardfacing and any other electrodes as manufactured by The Lincoln Electric Company at Cleveland, Ohio.

***Blaw-Knox Company, Pittsburgh, Pa.,** construct fusion welded equipment for high pressures and have all facilities to meet every requirement of the A. S. M. E. Code. They are experienced in the fabrication of the various special corrosion resisting alloys and their engineering department designs Process equipment to meet specific purchaser's requirements. In addition the diversified interests of their subsidiary companies put them in an unusual position to serve the equipment user.

* These companies also carry a Sustaining Member in the Society.

The Buda Company, Harvey, Ill., established 1881. Company manufactures a wide and complete line of railway equipment and supplies including such products as motor cars, track drills, jacks, frogs and switches. Buda also manufactures Diesel, natural gas, gasoline and butane engines for all types of marine service; for trucks, busses and taxicabs; for portable industrial machinery such as shovels, cranes, welders, compressors, ditchers, graders, and rollers; for stationary industrial service; and for all types of oil field service.

***Edw. G. Budd Manufacturing Company, Philadelphia, Pa., and Detroit, Michigan.** Originator of ALL STEEL bodies for automobiles, this company manufactures automobile bodies and parts, truck bodies and chassis frames, tools, jigs, fixtures and dies of all types. By the use of the Budd System of Light-weight Construction—high-tensile stainless steel welded by the exclusive SHOTWELD process—Budd also builds light-weight railway cars, bus, truck, and trailer bodies, marine and airplane structures.

***T. A. Canty, Baltimore, Md.,** district manager for The Lincoln Electric Co., world's largest manufacturer of arc welding equipment, Cleveland, Ohio. Has had wide experience in application and use of arc welding, serving users in Maryland and Virginia since 1921. Organization, built up during 16 years, features engineering service by qualified welding technicians. Maintains one of the largest stocks of arc welding equipment and supplies, consisting of Lincoln welders and electrodes, in southeastern United States.

***Carnegie-Illinois Steel Corporation, Carnegie Building, Pittsburgh, Pa., and 208 South LaSalle Street, Chicago, Ill.,** a subsidiary of United States Steel Corporation. Combined in this one unit are the manufacturing facilities of the former Carnegie, Illinois and Lorain Steel Companies, and American Sheet and Tin Plate Company. Production covers a vast range of rolled, forged, cast and fabricated steel products—plates, shapes, bars, rails, sheets, tin plate, stainless and heat resisting steels, high-tensile steels, all types of alloy steels and special sections of every description.

A. M. Castle & Co., Chicago, Ill. General offices and warehouses at Chicago, Ill., branch offices and ware houses at Los Angeles, San Francisco and Oakland, Cal., and Seattle, Wash. A complete line of steel and allied products is carried in stock at each warehouse, among the items carried being structural sections, bars and bar shapes, plates, sheets, cold finished bars, alloys, stainless steels, welding rods, tubing and pipe, wire rope and a number of specialities.

***The Champion Rivet Company, of Cleveland, Ohio, Western Plant at East Chicago, Indiana,** is one of the pioneer companies in the metal joining industry. Over forty years ago the late Mr. David J. Champion founded the company and pioneered in the manufacture of steel rivets. During the past six years they have been making a complete line of heavily coated welding electrodes known to the trade as Champion Red Devil, Blue Devil, Gray Devil shielded arc electrodes. They are represented in practically all the principal cities of the United States.

Chicago Bridge & Iron Company, 37 West VanBuren Street, Chicago, Ill. The Company employs welding in the fabrication of vessels conforming to Paragraph U-68 of the A. S. M. E. Code for Unfired Pressure Vessels at its Birmingham plant and vessels fabricated in Paragraph U-69 of the Code at its Birmingham, Chicago and Greenville, Pennsylvania, plants. Also in assembling riser pipes for elevated tanks, tower rods, manholes, nozzles, etc. In field, welding is employed in building of flat bottom storage tanks for various portions of elevated steel tanks and other steel plate structures.

***Cleveland Electric Illuminating Company** serves Cleveland and 133 other communities in five counties in northeastern Ohio with electric light and power. Territory embraces 1700 square miles extending 100 miles along south shore of Lake Erie from Pennsylvania State Line westward to Avon. Population 1,300,000. Customers 324,000. Power is generated in three modern pulverized fuel steam generating plants, one at Ashtabula, another in Cleveland and a third at Avon. These plants have total generating capacity of 527,500 kilowatts and are interconnected by a 132,000 volt transmission system. Company also provides central steam heating service for the downtown area of Cleveland.

***Combustion Engineering Company, Inc., 200 Madison Avenue, New York,** manufacturers of all types of Water Tube and Fire Tube Boilers, Pulverized Fuel Burning Systems and Stokers; also Water-Cooled Furnaces, Superheaters, Economizers and Air Heaters; fabricators of Pressure Vessels, Tanks, etc., welded or riveted, in carbon, alloy or clad steel; also platework.

Commonwealth Edison Company, 72 West Adams Street, Chicago, Illinois. Public utility operating company engaged in the production, purchase, transmission, distribution and sale of electric energy for domestic, commercial, industrial, transportation and municipal purposes, in the City of Chicago. Also engages in the sale of electrical appliances to promote the development of its electricity supply business.

***Crane Co., 836 So. Michigan Ave., Chicago, Ill.** Crane Co. maintains fabricating shops in Chicago, Bridgeport, Birmingham, Los Angeles and other Branches in the larger cities throughout the country. In Chicago complete facilities are available for bending, lapping, welding, and heat treating of piping in both carbon and alloy steels for use in steam power plant, oil refinery, oil and gas transmission, chemical and various other industries. Pressure vessels are also fabricated by welding under A. S. M. E. Code requirements.

***The Detroit Edison Company** serves Detroit and 298 other communities in Michigan with light and power. Territory, 7587 square miles extending northerly 155 miles from Ohio line, westerly 95 miles from International Boundary. Population 2,460,000. Customers 625,000. Has 4 large and 3 small steam plants, 6 small hydro plants. Generating capacity 930,800 kilowatts. The Company provides gas service to 10,500 customers, and provides steam heating in downtown Detroit.

***C. H. Dockson Company of Detroit, Michigan,** manufactures Welding and Safety Equipment. Outstanding products are Hi-Speed Oxyacetylene Welding and Cutting Torches, Pressure Regulators of single stage design but of double stage performance, Siamese Welding and Cutting Hose, Dowmetal Goggles, adaptable to every hazard, Electrode Holders and the new TURRETYPE Arc Welding Shields formed from a single sheet of black vulcanized fiber.

***Downingtown Iron Works, Downingtown, Pa.,** welded plate fabrication of every type for all purposes of ordinary carbon steel, chrome nickel, chrome alloys, nickel, nickel clad steel, stainless clad steels, Monel metal to form: acid tanks, air pressure tanks, grease tanks, jacketed tanks, pressure tanks, tanks for storage of chemicals, buoys, pontoons, stills, calandrias, fermentation tanks, storage bins, etc.

Duraweld Metal Products Corporation of Long Island City, N. Y., has developed an outstanding product in the hard surfacing of metal. In addition to the hard surfacing welding rods which are uncoated and can be applied either with the electric arc or oxyacetylene torch, they have recently added a line of fine tool holder bits and lathe center plugs which are finding much favor among discerning machinists and master mechanics. The corporation is now represented in a number of important trade centers in the United States.

Electro-Motive Corporation La Grange, Ill., builds self-propelled motor cars for branch line service for railroads and oil electric locomotives of larger sizes for main line service. Manufactures high speed oil electric locomotives up to 5400 hp. for streamlined trains, and 600 and 900 hp. switch engines of welded construction. Road locomotives are entirely of welded construction made of various pressings and structural shapes and mostly of high tensile alloy steel.

***The Fibre-Metal Products Company, Chester, Pa.** Manufacturers of Welding Accessories and Industrial Face Protection. Designers and Inventors of Fabricated and One-Piece Welding Helmets, Hand Shields, Chrome Leather Helmets, Sand Blast Helmets, Metallic and Carbon Arc Electrode Holders from 200 to 600 amperes. Distributors of General Electric Arc Welding Cable and Bausch and Lomb Optical Co.'s Welding or Filter Glass. Also, specializing in intricate forming and stamping of Vulcanized Fiber.

The Fidelity and Casualty Company of New York, 80 Maiden Lane, N. Y. This company, for more than sixty years, has provided insurance protection for power plants throughout the United States and Canada. Every boiler and machinery policy-holder receives an engineering service which provides for periodic inspections of the insured equipment. This alone is of inestimable value in that defects and improper conditions are discovered before causing possible loss of life and property. The engineers are equipped with the latest type of instruments for properly inspecting the insured equipment.

Krembs & Company, 669 West Ohio Street, Chicago, Ill. Manufacturing Chemists since 1875, Consulting Welding & Brazing Engineers, Manufacturers of the "FLUXINE" BRAND line of fluxes—a complete line for Brazing, Welding, Silver and Soft Soldering every metal and alloy. Each of these fluxes was developed to meet the particular requirements they are recommended for. "BRAZ-WELD" BRAND SELF-FLUXING RODS, "OXY-DYZ-ENE" brand (Highest strength) oxidizing agents. These products are sold by leading jobbers and distributors throughout this country and abroad.

Foote Mineral Company, Office, 1609 Summer Street, Philadelphia, Pa. Factories at Asbestos, Pa. and 4041 Ridge Avenue, Philadelphia, Pa. Manufactures coating materials and fluxes for steel, aluminum and hard surfacing electrodes. These products include lithium fluoride for aluminum welding, tungsten metal powder and powdered ferro alloys for hard surface welding and powdered rutile, manganese dioxide, iron oxide, asbestos and other coating materials for steel welding.

***Frick Company, Waynesboro, Pa.** This company builds ice-making and refrigerating machinery and air-conditioning equipment and uses electric and oxyacetylene welding in making various parts of their systems. Pressure vessels, such as ammonia, carbon dioxide and Freon receivers, oil separators, accumulators, condensers and brine coolers are all of welded construction; also continuous pipe coils, headers and bends are welded. They use welding for tanks, pans and for repairing castings and are interested in the art in all its phases.

***Gardner Publications, Inc. (Modern Machine Shop), 431 Main Street, Cincinnati, Ohio,** a monthly publication directed to mechanical executives in charge of maintenance or manufacturing departments, or both, in metal-working plants. Member of the Controlled Circulation Audit. Circulation more than 28,000 copies each issue. MODERN MACHINE SHOP leads all monthly publications on total number of advertising pages being carried during 1937 and presents to its readers more pages of selling messages per issue than any other magazine in the world.

General Electric Company, General Offices, Schenectady, N. Y. Manufactures a wide variety of arc welding and other electrical equipment. Single-operator direct-current generator for belt or direct drive, or direct connected to electric motor or gas engine. Multi-operator direct current welding sets. Rectifier arc-welders. Resistor arc-welders. Alternating current arc-welders, transformer type. Atomic hydrogen arc- welders. Automatic arc-welders for the direct current, alternating current and atomic hydrogen welding processes. Welding accessories, including cable and electrodes.

***Handy & Harman, 82 Fulton Street, New York City.** Products include, Rolled Sterling Silver, 999 "PLUS" Fine Silver Anodes, "Special Refined" Grain Silver, Gold and Silver Wire, Karat Golds and Gold Solders, Gold Bars, Silver Bars, Platinum Metals, "Handy" Silver Solders. Sil-Fos and Easy-Flow low temperature brazing alloys, Handy Flux. We also refine all kinds of manufacturers' scraps, sweeps and other waste containing precious metals, also old gold, silver and platinum.

***Harnischfeger Corporation, 4400 W. National Avenue, Milwaukee, Wisconsin.** Manufacturers of the P & H Hansen Arc Welder, famous for its single current control and patented Stabilized Arc. Available in capacities from 100 to 600 amperes in the electric motor driven machines and 150 to 400 in the gas engine driven welders. Welding positioners, all welder accessories and P & H Smootharc welding rod are also produced, making Harnischfeger one of the leaders in the Welder Industry. Offices and service facilities in all principal cities.

***Harris Calorific Company, 5501 Cass Avenue N. W., Cleveland, Ohio.** Pioneer manufacturers of Welding and Cutting torches for utilizing Oxygen in combination with Acetylene, City Gas, Natural Gas, Propane and other Fuel Gases; famous "Multi-Stage" and other Regulators for controlling either high or low pressure gases, "Gasavers" and Automatic Acetylene Generators.

***Hartford Steam Boiler Inspection and Insurance Company, Hartford, Connecticut.** Insurers against loss from accidents to boilers, unfired pressure vessels, and power apparatus. The Company endeavors to reduce the likelihood of accidents by means of periodic inspections of each insured object and it also makes shop inspections of pressure vessels of all kinds during construction. It checks designs of pressure vessels for insurance to make certain that they comply with the several construction Codes and State Laws.

***Hendrie & Bolthoff Mfg. and Supply Co., 1601-39 Seventeenth Street, Denver, Colorado,** distributors of Machinery and Supplies for Mining, Sawmill, Machine Shop, Builders, Hardware, Heating, Electrical, Automotive and Radio. Large stocks of General Electric, Hollup, Stoody, Roebling, Oxweld and Purox Welding Rods, also D.C. and A.C. Welding Sets and Spot Welders. Purox Oxyacetylene Equipment and Metalspray Apparatus Main Offices and Warehouses, Denver, Colorado; Branch Warehouses and Stores, Pueblo, Colorado, Albuquerque, New Mexico; Greeley, Colorado; Deadwood, South Dakota; Colorado Springs, Colorado.

***The Hobart Brothers Co., Hobart Square, Troy, Ohio.** Manufacturers of Hobart "Simplified" Arc Welders featuring Remote Control; Compact, End-Bearing Design; All Welded Steel Construction; Selective Stabilization; End to Center Ventilation and "Motor Horsepower Control." Built for operation with electric or gasoline power or for connection to customer's own power unit. Portable and stationary models are offered to meet or exceed N. E. M. A. or U. S. NAVY specifications in 75, 100, 150, 200, 300, 400 and 600 ampere ratings.

Hollup Corporation 3357 West 47th Place Chicago, Illinois. Manufacturers of (1) the SUREWELD line of Protected Arc electrodes, (2) Wanamaker and Rex lines of Coated and Processed electrodes, (3) Gas welding wire and supplies. A welding rod for every job in electric and oxy-acetylene welding. Engineering service to users of welding available from Chicago and other principal cities.

***Hudson Motor Car Company, Detroit, Mich.,** manufacturer of Hudson passenger cars and Hudson Terraplane business cars. Built to excel in style, performace and long life. Passenger Cars: Hudson Terraplane, 117-inch wheelbase, 96 or 101 horsepower; Hudson Six, with Six Star Motor, 122-inch wheelbase, 101 or 107 horsepower; Hudson Eight, 122 and 129-inch wheelbase, 122 horsepower. Business Cars; 117 and 124-inch wheelbases, 96 or 102 horsepower. America's No. 1 Value Cars.

Ingalls Iron Works, Birmingham, Ala. The Ingalls Iron Works Company is prepared to design, fabricate and erect structural steel of all descriptions and for all purposes; tanks of all descriptions and sizes for all purposes other than where A. S. M. E. Class 1 is required; stacks, breeching, ducts, bins, hoppers; also, to manufacture barges, dredges, carfloats and other floating equipment; standard storage tanks, and barrels. Plants are located at Birmingham, Alabama; North Birmingham, Alabama; Decatur, Alabama; Chickasaw, Alabama; and Verona, Pennsylvania.

***Industrial Publishing Co., 812 Huron Road, Cleveland, O.,** publishes two publications in the welding field—one called *Industry and Welding*, is distributed to 27,000 shop men; and the other *Fabrication Progress* is sent to 9000 executives. The latter deals with the economic side of welding, and the former the technical side. In the monthly will be found technical articles dealing with all types of ferrous, non-ferrous and alloy welding. *Fabrication Progress* deals with products successfully fabricated by using the welding process.

***Inland Steel Company, Chicago, Illinois.** Open-Hearth Steel Manufacturers: Semi-Finished Products, Merchant Bars, Concrete Reinforcement Bars, Plates, Inland 4-Way Floor Plates, Shapes, Steel Sheet Piling, Rails and Accessories, Hot and Cold-Rolled Sheets and Strip, Galvanized Sheets, Zinc-Alloy Sheets, Cold Reduced Tin Plate, Tin Mill Black Plate, Terne Plate, Rail Steel Products, Inland Copper-Alloy, Inland Hi-Steel. Bulletins gladly sent upon request. District Offices: Detroit Milwaukee, Kansas City, St. Louis, St. Paul.

***The International Nickel Co., Inc., 67 Wall Street, New York, N. Y.** Manufacturers of Inco Nickel Monel and Inconel welding wire for oxyacetylene welding; Inco Monel gas welding and brazing flux and Inconel gas welding flux for oxyacetylene welding of Monel and Inconel, respectively; flux coated electrodes for carbon and metallic electric arc welding of nickel, nickel-clad steel, Monel and Inconel. Producers of nickel, Monel and Inconel bars, rods, sheets, strip, tubes, pipe and forgings.

***Jackson Electrode Holder Co., 6553 Woodward Ave., Detroit, Michigan.** Manufacturers of completely insulated electrode holders for use in electric arc welding. Insulation consists of heat-resisting shields, protecting all metal parts, eliminating flashes due to contacting holder with grounded metal. Features claimed are safety, avoidance of work spoilage, long life, coolness, lightness. Rod capacities vary from $^1/_{16}$" to $^3/_8$" currents 200 to 500 amps. The line also includes three bare type models of corresponding capacity.

***Johnson Supply Co., 1414 Wazee St., Denver, Colo.** We are the exclusive distributors of Lincoln Electric Company, welding equipment and electrodes in Colorado, part of Wyoming, western Nebraska, two Western terra counties of Kansas and as far south as Albuquerque, New Mexico. We employ a welding technician, and have four men calling on the trade constantly. We specialize entirely in Lincoln Electric products.

J. B. Klein Iron & Fdy. Co., Oklahoma City, Okla. Manufactures gray iron, brass and aluminum castings, etc.

The M. W. Kellogg Company, in its Jersey City plant, manufactures by the electric arc process Class I vessels for the oil refinery and chemical industries, and also high pressure penstock, steam and process pipe lines. Carbon steel, as well as alloy steel vessels of many types, are fabricated. In addition to the shop work, the Company designs and constructs cracking units, polymerization units and oil dewaxing units for the oil industry. Many Kellogg units are in successful operation all over the world. This Company also constructs radial brick chimneys.

***The Lincoln Electric Company, Cleveland, Ohio.** Specializes in manufacture of "Shield Arc" dual continuous control welders, single-operator variable voltage machines in motor-driven, gas-engine and belt-driven models of 100 to 600 amperes, capacities N. E. M. A. rating; automatic welders utilizing "Electronic Tornado" process of carbon arc welding; electrodes for all applications of arc welding; accessories and supplies of all types for manual and automatic arc welding; also "Linc-Weld" motors for A.C. current; starters and controls.

***The Lincoln Electric Railway Sales Company, Marshall Building, Cleveland,** Agents and Sole Railway Representatives of The Lincoln Electric Company, specializing in the sale of Lincoln Welding Machines, Electrodes, Welding Accessories and Linc-Weld Motors for A.C. Current to all railroads of the United States. Complete engineering and practical service available for customers and prospects.

***The Linde Air Products Company, 30 East 42nd Street, New York, N. Y.,** a Unit of Union Carbide and Carbon Corporation, produced the first commercial oxygen by the liquid air process in America and is the world's largest manufacturer of this product. Its 69 plants and 98 warehouse stocks serve every industrial community in the country. A complete engineering and practical service is maintained to assist customers in applying the oxyacetylene process effectively. Linde also markets Oxweld, Prest-O-Weld and Purox welding and cutting apparatus and supplies, Prest-O-Lite dissolved acetylene, Union carbide, Carbic, Carbic acetylene generators and Carbic flood lights.

***Ludlum Steel Co., Watervliet, N. Y.** Makers of Fine Steels—Tool, High Speed, Stainless, Nitralloy, Hollow Drill, Ball and Bearing, Magnet, etc., in the form of Hot and Cold finished bars, wire, forgings, etc. Also a manufacturer of Hard Surfacing Welding Rod. Plants at Watervliet and Dunkirk, N. Y. Stainless Foundry at Buffalo, N. Y. Subsidiaries—Forging and Casting Corp., Detroit, Mich., and Wallingford Steel Co., Wallingford, Conn.

***Lukenweld, Inc., Division of Lukens Steel Company, Coatesville, Pa.** Designers and fabricators of welded steel structures for use in various types of machinery and equipment. Lukenweld, Inc., has specialized in the design and construction of the difficult and unusual in welding, such as welded steel Diesel engine frames; underframes and other structural parts for high speed trains; railway truck frames, bolsters and spring planks, continuous welded frames for presses; welded steel gears, and paper machine driers.

Machinery & Welder Corporation, offices and warehouses located at Chicago, Ill., Moline, Ill., St. Louis, Mo., Kansas City, Mo., and Milwaukee, Wis. Distributors of welding equipment including oxyacetylene AC and DC, stationary and portable. Handle electrodes, gas welding rods for manual and automatic welding, automatic welding heads, equipment for seam, flash and spot welding, atomic hydrogen, automatic oxyacetylene flame cutting, acetylene generators. Carry at all warehouses a complete stock of safety clothing, welding supplies, and accessories.

***Magnolia Airco Gas Products Co., P. O. Box 318, Houston, Texas.** Produces and supplies Airco high-purity oxygen, nitrogen, hydrogen, argon, neon, helium, krypton and xenon, atmospheric gases, acetylene, carbon dioxide in liquid and solid ("DRY-ICE") form, carbide, Airco-DB welding and cutting torches, pressure regulators and automatic welding and cutting machines, acetylene generators, welding and cutting supplies, gas welding rods, electrodes and Wilson Electric Arc Welders. Maintains a nation-wide oxyacetylene welding and cutting and electric arc welding supply service distributed through 6 district sales offices, 6 distributing stations, and 5 plants.

P. R. Mallory & Co., Inc., Indianapolis, Indiana. Manufacturers of resistance welding electrodes for all kinds of similar and dissimilar metals. Elkonite, Elkaloy, Mallory 3, 53, 73, 84 and 100 metals are electrode materials and Soldaloy #8 is soldering iron tip material. All are alloys of extreme hardness and wear resistance with high electrical and thermal conductivity. Recent contribution to literature on welding is the book, "Engineering Data—Resistance Welding Theory and Practice."

***Maurath, Inc., 7309 Union Ave., Cleveland, Ohio.** George A. (Whitey) Maurath, President and General Manager. Manufacturers of stainless and heat-resisting electrodes, specializing and perfecting commercial production and use of electrodes. They have a complete plant controlling all the operations from the hot mill to the finished product. With their capacity, their own wire mill places them in a unique position for producing on short notice all the various high alloys. Their product is used world wide.

***Metal & Thermit Corporation, 120 Broadway, New York, N. Y.,** manufacturers of materials and equipment for Themit welding of heavy sections of various metals: railroad and street railway rails, housings, frames and beds of heavy machines, crankshafts, flywheels, steel mill rolls and pinions, etc. Exclusive Sales Agents for Murex heavy coated electrodes for arc welding: mild steel, manganese, stainless, nickel, carbon molybdenum, chrome molybdenum steels, etc. Producers of M & T metals and alloys: ferro titanium, vanadium, pure chromium, etc.

***The McKay Company, Pittsburgh, Pennsylvania.** Plants at McKees Rocks (Pittsburgh), Pa., and York, Pa. Manufacturers of Shielded Arc Welding Rods, Automobile Tire Chains, Commercial and Industrial Chain of all descriptions, Outdoor Metal Furniture, and Chrome Indoor Metal Furniture.

***The Murray Corporation of America, 7700 Russell St., Detroit, Mich.,** manufacturers of automobile passenger and commercial bodies; automotive stampings, fenders, chassis frames; automotive mouldings, rolled sections; automotive cushion springs. Representive, L. C. Hill, manufacturing manager.

National Carbide Corporation, Lincoln Building, opposite Grand Central, New York, N. Y. Manufactures and distributes National Carbide through approximately 250 warehouses located in all principal cities. National Carbide is placed in 100-lb. heavy steel drums, for use in generating acetylene, for oxy-acetylene processes and for house lighting, and also in 100-lb. and 25-lb. drums and 10-lb. and 2-lb. cans for use in miners' and other carbide lamps. Also distributors of railroad and contractors' carbide lights of all description.

National Cylinder Gas Company, 205 West Wacker Drive, Chicago, Illinois, operates directly and through subsidiaries, thirty-two plants, and engages in the manufacture and sale of oxygen and acetylene, and sells gas and electric welding and cutting equipment and supplies, as well as hydrogen, nitrogen, propane and CO-2. This organization is particularly active in promoting the oxygen jet cutting process and the flame hardening of metals.

***New Orleans Public Service Inc., 317 Baronne Street,** Public Utility generating and distributing electrical energy; distributing gas and operating combined street car and bus system, all in the city of New Orleans.

***Northern States Power Company, Minneapolis, Minnesota.** The utility system centers about Minneapolis and St. Paul, an area with a population of 800,000 people. In addition smaller cities and towns are served throughout the surrounding territory in Minnesota, North and South Dakota, Wisconsin and Illinois. A total of 600 communities is supplied with service, having an estimated total population of 1,500,000. Twenty-one steam electric stations and twenty-six hydro electric stations, mostly interconnected, provide a dependable source of power supply.

The Ohio Public Service Company, Hanna Building, Cleveland, Ohio. Division Offices: Alliance, Ohio; Ashland, Ohio; Elyria, Ohio; Lorain, Ohio; Mansfield, Ohio; Massillon, Ohio; Port Clinton, Ohio; Sandusky, Ohio; Warren, Ohio. Generatng Plants: Lorain, Mansfield and Warren. Producers and distributors of electric power for home, commercial and industrial use. Unlimited capacity of reliable and economical power supply for all industrial requirements.

***Page Steel and Wire Division of American Chain & Cable Co., Inc., General Offices, Monessen, Pennsylvania.** Manufacturers of Page Armco and Page Steel Electrodes and Gas Welding Wire; Page Hi-Tensile Electrodes which produce welds conforming to A. W. S. specifications; Hi-Tensile Gas Wire; Page-Allegheny Stainless Steel Electrodes and Gas Welding wire; also manufacturers of rods, high and low carbon wires, special analysis wires, bond wires, strand, stainless steel wire for mechanical purposes; farm fence, industrial and residential chain link fence; panel partitions, traffic tape and stainless steel tennis nets.

***Penton Publishing Company, Penton Building, Cleveland, Ohio.** Publishes business papers serving metalworking industries, including Steel, a weekly magazine, dealing with production, processing, distribution and use of iron, steel and supplementary materials; Machine Design, monthly technical journal covering a design as it affects engineering production sales; The Foundry, monthly technical journal found wherever metals are cast;

Daily Metal Trade, daily business paper of ferrous and non-ferrous industries; and Abrasives, a monthly technical journal devoted to grinding operations.

*The **Petroleum Iron Works Company, Sharon, Pa.** PIW fusion-welded pressure vessels of all kinds constructed of carbon or alloy steel for high pressure, high temperature and corrosion resistant service built in accordance with the A. S. M. E. and A. P. I.-A. S. M. E. Unfired Pressure Vessel Codes requiring X-raying of seams and stress relieving; fabricators and erectors, general steel plate work, welded or riveted; oil refinery equipment; chemical processing equipment. Also manufacturers of steel barrels and drums for liquids, semi-liquids and solids.

***Philadelphia Electric Company.** Philadelphia, its suburbs and extensive surrounding territory, comprising a population of over 2,700,000 and covering an area of more than 1600 square miles, is served entirely with electricity by the Philadelphia Electric Company. Gas is also supplied in the suburbs and surrounding large industrial districts. Steam is served as well in center-city Philadelphia. The company enjoys an outstanding reputation for aggressiveness, rate-reducing policies and happy customer relations. It is famous for the erection of Conowingo Dam, which ranks next to Niagara Falls among the hydro-electric developments of the United States.

***Pittsburgh Piping & Equipment Company, 10—43rd Street, Pittsburgh, Pa.** Manufacturers of Fabricated Piping and Pipe Fittings; Fabricating and installing of complete piping systems for central stations and industrial plants, including welding of carbon and alloy steel piping for the highest pressures and temperature. Manufacturers of creased bends and superflexible corrugated pipe tangents for expansion bends. Manufacturers of all types of stainless steel process piping and stainless steel fittings.

***Pittsburgh Steel Company, Grant Building, Pittsburgh, Pa.** Manufacturer of wire and wire products, and seamless tubing. Electric and gas welding wire a specialty. Special finishes on welding wire for coated electrodes produced according to customers' individual coating requirements. Specializing in plain carbon, mild alloy and Stainless Steel wire for all manufacturing purposes. Also manufacturer of reinforcing fabric, woven and welded wire fence and nails. Seamless tubing for oil country goods, boiler tubes and special fabrication of tubular parts. A complete line of carbon and S. A. E. steels.

***The William B. Pollock Company, 101 Andrews Avenue, Youngstown, Ohio.** Established in 1863: Engages in the Engineering, Fabricating and Erecting of Steel Plate Construction for Blast Furnaces, Steel Plants, Oil Refineries, Chemical Plants and Allied Industries. Also in the manufacture of Hot Metal Cars, Cinder Cars and Ladles. The Pollock Company pioneered the use of Welded Rolled Steel Construction in place of castings, and for twelve years have built large and small machinery parts for others.

***Pullman-Standard Car Manufacturing Company, Chicago, Ill.,** producer of railway passenger and freight cars, of conventional or lightweight design, fabricated by riveted welded methods. Also producers of streetcars and trolley buses. Makers of wheels, equalizers and forged and machined parts for railway use. Has passenger car plants in Chicago and Worcester, Mass.; freight car plants in Hammond, Ind., Michigan City, Ind., Butler, Pa., Birmingham, St. Paul, Minn., Canton, Ohio, Baltimore, and Richmond; and wheel foundries in Houston, Tex., and New Orleans.

***The Pusey and Jones Corporation, Wilmington, Delaware,** has been incorporated since 1848. The building of ships and paper making machinery are the leading classes of manufacture. All types of ships up to 400 feet in length have been built at the plant and large yachts are their specialty. In the paper machine division they have built the largest and fastest news print and kraft machines in the country.

***Republic Steel Corp., Cleveland, Ohio.** Subsidiaries include Truscon Steel Co., Berger Manufacturing Co., Union Drawn Steel Co., Steel and Tubes, Inc., and Niles Steel Products Co. Third largest producer of iron and steel products. Specialties include Enduro Stainless Steel, Toncan Iron, Agathon Alloy Steels, Republic Double Strength Steel, Silicon Steels, and Republic Electric Resistance Weld Pipe. Recent contribution to literature on welding is the free booklet, "The Welding of Enduro Stainless Steel."

John A. Roebling's Sons Company, Trenton, N. J. Manufacturers of welding wire, high- or low-carbon or high manganese steel; reels for automatic or semi-automatic welding equipment; electric arc-welding machine cable; electrode holder cable. Manufacturers of wire rope, wire rope slings for heavy or light duty, bare or insulated copper wires and cables. Engineering data and catalogs sent upon application.

Reed Roller Bit Company, P. O. Box 2119, Houston, Texas. Manufacturers of Rotary Drilling Tool, Engines and Pumping Units. Among these products are: DK Roller Bits for all formations, Rotary Reamers, The new Kor King Core Drill for both soft and hard formations, Tool Joints, Welded-in Blade drag bits with 2, 3 and 4 blades, both pin and box. The B-R Drilling and Coring bit with Wire Line Retractable Core Barrel. Replaceable Blade Bit, Drill Collars, Substitutes and Basket Bits.

***The Benjamin F. Shaw Company, 503 East Second Street, Wilmington, Delaware,** are Industrial and Power Plant Piping Fabricators. Cutting, threading, flanging, bending and welding are some of the operations performed in their fully equipped shops. Certified welders are employed in the welding shop. The various alloys in all forms are fabricated, as well as piping for all pressures and temperatures. A modern shop and an experienced personnel are your assurance of a satisfactory job.

***Shell Oil Company, 100 Bush Street, San Francisco, Calif.,** producers, refiners and marketers of petroleum products. Distributing facilities in California, Oregon, Washington, Nevada, Arizona, Utah, Idaho, Montana, Wyoming, New Mexico, British Columbia and Territory of Hawaii. Manufacturers and marketers of Shell Brand of gasoline, distillates, kerosene, lubricating oils, greases and fuel oils. Refineries at Martinez, Coalinga, Dominguez and Wilmington, Calif. Marine loading terminals, San Francisco Bay, San Pedro Harbor, Portland and Seattle.

***A. O. Smith Corporation, Milwaukee, Wisconsin.** Manufacturers of welded steel products. Pioneered electric welded high-pressure vessels for oil cracking and now the leading supply of such vessels. Pioneered electric welded line pipe for gas, oil and gasoline. Manufacturers of high yield casing for oil wells. Leading producer of pressed steel automobile frames and glass-lined steel tanks for the brewing and food industries.

***S. Morgan Smith, York, Pa.** Founded in 1877 by one of the pioneer builders of hydraulic turbines, S. Morgan Smith Company has grown to be one of the world's major producers of hydraulic turbines and related equipment. The company operates a modern manufacturing plant equipped with a wide variety of machine tools enabling it to produce large special machinery in addition to its regular products, and also a modern welding department especially equipped to handle large and complicated welding jobs.

***Standard Oil Company of California, 225 Bush St., San Francisco, Cal.,** is engaged in all phases of the oil industry, with distributing facilities throughout the Pacific West as well as in Alaska, Hawaii and through a subsidiary in Mexico,utilizes welding to a high degree throughout the organization, and has made a number of contributions to the development of welding equipment and technique; manufacturers Calol Cutting Gas, a liquefied natural gas product widely used for metal cutting.

***Steel Sales Corporation, 129 So. Jefferson St., Chicago, Ill.,** large Midwestern Warehouse distributors of Steel and Non-Ferrous metals for all manufacturing purposes. Extensive stocks of welding rod, gas and electric; Welding Supplies and Silver Solders. Branch Offices and Warehouses at Detroit and St. Louis. Offices at Milwaukee and Minneapolis. Personnel inincludes Welding Engineers and men thoroughly experienced in all manufacturing practices.

***Stoody Company, Whittier, California.** Manufacturers of hard-facing materials, welding rods, grinders, electrode holders and sand blasting nozzles. Hard-facing materials include: Borium, Tube Borium, Cobalt Borium, Borod, Stoodite, Stoody Self-hardening Rod, Silfram and the Numbered Stoodites: 45, 54 and 63. All Stoody hard-facing materials, with the exception of the Numbered Stoodites which at present are for acetylene application only, are supplied either for electric or acetylene application.

Sun Oil Company, Philadelphia, Pa., established 1886. Producer, refiner and marketer of Blue Sunoco Motor Fuel, Sunoco "Mercury Made" Motor Oil, Sunoco Automotive Greases and a wide variety of petroleum products under the trade names Sunoco and Sunoils. Holds 26 U. S. patents on its exclusive Mercury Process of refining. Has oil fields in 9 states. Operates refineries at Marcus Hook, Pa., Toledo, Ohio, and Yale, Oklahoma. Has 86 distributing plants in Eastern U. S. and markets its products in approximately 45 different countries.

***Sun Shipbuilding & Dry Dock Company, Chester, Pa.** Ships' hulls and equipment completely or partially welded. Welded steel products for oil refineries, chemical plants and kindred industries made to order for individual requirements. The facilities of this plant are unlimited for welded equipment as listed above and include automatic welding, X-ray and stress-relieving equipment for manufacturing largest units that can be shipped by rail or water. Extensive experience in manufacturing to the requirements of A. S. M. E. and A. P. I.-A. S. M. E. Codes.

*The Taylor-Winfield Corporation, largest manufacturer of electric resistance welding machinery in the United States, has its main plant and home office in Warren, Ohio, also maintains a large branch assembly plant as well as a separate fabricating division in Detroit, Michigan. Backed by forty years' experience in design and construction of all types of resistance welders, the company today offers a wide line of spot, projection, butt flash and roller seam welding units.

*Third Avenue Railway System, 130th Street and Third Avenue, New York, N. Y. The Third Avenue Railway System operates 250 miles of single track in New York City and municipalities north of it, carrying 300 million passengers. It also operates 140 route miles of buses carrying 50,000,000 passengers annually. Welding by electric arc, gas and thermit processes is extensively used in its shops and on its tracks. It regards the work of the AMERICAN WELDING SOCIETY of great value to the transit industry and, therefore, carries a sustaining membership in the Society.

Thomson-Gibb Electric Welding Co., Lynn, Massachusetts. This company manufactures a complete line of equipment based on the Thomson or Resistance Process of Electric Welding. The line includes manual and automatic Spot, Flash and Butt Welders as well as Seam, Press, Projection, Spotmatic, Portable Gun and Special Welding Machines. They also specialize in the development and production of equipment for resistance or induction heating.

Torchweld Equipment Company, Chicago, Illinois. Manufacturers of TORCHWELD Patented Non-Flash Welding and Cutting equipment for every welding or cutting problem—in aeroplane or automobile manufacturing and repair, railroad shops, oil fields, refineries, mines and steel mills, or any other place where gas welding and cutting is done. All standard sizes and kinds of welding rods, both gas and electric, as well as welding goggles, gloves, flame lighters, electrode holders, electric hand-shields and helmets are ready for prompt delivery.

*Treadwell Construction Company, Midland, Pa., designers and builders of heavy steel construction. Steel Plant Equipment including Ladles for Open-Hearth and Blast Furnaces, Welded Pipe, Processing Equipment, Homogeneous Bonded Lead Lined Vessels or Tanks, also Lead Covered Copper Coils. Boats, Barges, Caissons and Dredges. All types of Floating Equipment for inland waters. Plant located on Ohio River, making it possible to ship by water items of dimensions greater than railroad clearances will permit.

*Una Welding, Inc., 1615 Collamer Avenue, Cleveland Ohio., Manufacturers of Una Welding Rods including a complete line of protected arc rods for manual welding, rods for automatic welding, high speed production rods, stainless steel rods and rods for low alloy high tensile steels and special steels. Una Automatic Welding Heads for high speed production. Unamatic Welding Process and Una-taper depositing protected arc weld metal. Una Welders for A.C. and D.C. operation.

*Union Carbide Company, 30 East 42nd Street, New York, N. Y., a unit of Union Carbide and Carbon Corporation, pioneered in the commercial production of calcium carbide. Today, Union Carbide is the standard generator carbide for domestic and industrial uses. Warehouse stocks are maintained in 247 cities throughout this country. Union Carbide is sold through The Linde Air Products Company, Union Carbide Sales Company and the Oxweld Railroad Service Company, all units of Union Carbide and Carbon Corporation.

*U. S. Rubber Products, Inc., 1790 Broadway, New York. Manufacturers of "U. S. Royal" Welding Cable with Tempered Rubber Sheath. The U. S. line includes Bare and Weatherproof Wire, Rubber Insulated Flexible Cords, Cord Sets, Soft Rubber Plugs, U. S. Royal Cords and Cables, U. S. Safecote Building Wire, Rubber Insulated Power Cable with braided, leaded or non-metallic sheath. Laytex Communication and Control Cables, Telephone Wire, and also many types of Rubber Insulated Wire and Cable for special applications.

*United Shipyards, Inc., 11 Broadway, New York, N. Y. Build and repair all types of floating equipment, owning and operating four shipyards in the Greater New York area. Possess completely equipped foundry and machine shops. Also feature building of welded barges on Kjekstad patented systems.

*Universal-Cyclops Steel Corp., Bridgeville, Pa. & Titusville, Pa. Specializes in the manufacture of welding wire in all analyses of stainless and electric furnace steels. Complete facilities for melting, rolling and cold working. Also makes a full line of stainless, tool and other special carbon and alloy steels in the form of ingots, billets, plates, sheets, bars, wire and strip—both hot and cold finished. Manufacturers of special steels for all purposes.

Victor Equipment Company—Welding Equipment Division—844–50 Folsom Street, San Francisco, California, is engaged in the manufacture of the complete line of all types and sizes of welding and cutting torches, cylinder manifolds, acetylene gas generators, pressure reducing regulators for all cylinder gases, including regulators for laboratory purposes with delivery pressure ranges as high as 3500 lb. psi. VICTOR equipments have been manufactured for over a quarter of a century.

*Virginia Bridge Company, Roanoke, Va., plants at Roanoke, Va., Birmingham, Ala., and Memphis, Tenn. Designers, fabricators and erectors of steel structures of all kinds, including buildings, bridges, tanks, turn-tables, smoke-stacks, stand-pipes, penstocks, gas holders, airplane hangars, steel railway cars, car parts and underframes, cranes and derricks.

*The Welding Engineer Publishing Co., 608 S. Dearborn Street, Chicago, Illinois, publishers of "The Welding Engineer" (a monthly magazine), and of the "Welding Encyclopedia," and the "Oxy-Acetylene Welder's Handbook." "The Welding Engineer" covers arc welding, gas cutting and welding, resistance welding, and thermit welding, features new developments and new uses of these various processes, as well as design information leading to the economical use of welding. Edited primarily for the fabricating executive, designer, engineer, welding foreman, supervisor and welding shop proprietor.

*Weldit Acetylene, Detroit, Mich. The Weldit Acetylene Company with general offices in Detroit, Mich. and branches in Chicago, Pittsburgh and Cleveland has specialized in the development of gas saving welding equipment, the latest being an automatic lever controlled welding torch for production welding. Although the weight is very light, the gas saving feature contained in the handle is most efficient. Other gas saving equipment includes the Weldit Gasaver, Weldit Automatic Blowpipe. A complete new catalog will be ready for distribution Jan. 1, 1938.

*The Wellman Engineering Company, 7000 Central Avenue, Cleveland, O., manufacturers of coal and ore handling equipment, automatic gas producers, charging machines, coke pushers, floating cranes, material handling equipment, general steel mill machine, and the "Williams" line of clam shell and drag line buckets. Works at 7000 Central Avenue, Cleveland, O.

*Western Electric Company, Inc., 195 Broadway, New York, N. Y. Manufacturers of telephones and telephone equipment for the companies constituting the Bell Telephone System; also purchaser of equipment and supplies for that system. Manufacturer or licenser of certain by-products related to the field of wire communication, including the Western Electric Sound Projection System for synchronizing motion pictures and sound; the Public Address System; electrical methods of recording sound; Permalloy type of high-speed submarine telegraph cable; radio broadcasting equipment; Western Electric Aviation Communication.

*Western Pipe & Steel Co., Los Angeles, Calif. Our two main plants, located at South San Francisco and Los Angeles, California, are well equipped for heavy plate, structural steel and light sheet fabrication. They are equipped with the latest X-ray equipment and stress-relieving furnaces for Class No. 1 welding. Principal products are heavy pressure vessels, electric welded pipe, welded and riveted tanks, bolted tanks, water well casing, structural steel and all types and descriptions of plate and sheet fabrication.

Westinghouse Electric & Manufacturing Co., East Pittsburgh, Pa. One of the world's largest manufacturers of equipment for the generation, distribution and utilization of electric power with a world-wide coverage of its products. Main works at East Pittsburgh. Service shops, dealer representation, warehouses and offices in all important cities. Welding is used extensively in its manufacturing processes. A pioneer in the development of electric welding methods, machines and materials for all industries and all purposes.

Whiting Corporation, Harvey, Illinois. Manufactures industrial products including electric traveling cranes; foundry equipment; pulverizing machinery; railroad shop equipment; stokers, evaporators, crystallizers and filters for the chemical industries. Welding is used extensively, especially in evaporator and filtration equipment. The company has also built a great variety of special heavy machinery on a contract basis. Its welding facilities are modern and it has had wide experience in welding various kinds of alloy steels, nickel and nickel-clad plate; also copper and bronze.

*Williams and Company, Inc., Pittsburgh, Cleveland, Cincinnati. Warehouse Distributors who know the welding business and

whose welding salesmen are experience-trained welders. They offer for immediate delivery from stocks the correct materials for any welding job. Stocks embrace Electrodes of the shielded arc, dust or dipped coated types for Steel, Cast Iron, Manganese Steel, Monel, Nickel, Inconel, Nickel-Clad Steel, Stainless Steels, Aluminum, Copper Alloys. Also Brazing Alloys, Fluxes, Carbon Plates, Welding Accessories, and Safety Equipment. Warehouse distributors of Westinghouse Flex Arc Welders.

Wilson Welder & Metals Co., Inc., 60 East 42nd Street, New York, N. Y. Manufacturers of a complete line of variable voltage welding machines, 75 to 800 Amperes, and constant potential multiple operator welding units, 500 to 3000 Amperes capacity and 150 to 1000 ampere A.C. transformer welders (conforming to N. E. M. A. Standards). Also a complete line of Wilson Color-tipt bare, coated and shielded arc electrodes and welding supplies. Machines distributed in all principal cities by the Air Reduction Sales Company.

***The John Wood Manufacturing Company, Inc., Conshohocken, Penna.** This company manufactures quality welded steel range boilers and tanks of every description. Back of that elusive word "quality" stands more than merely the desire to produce a good product. It involves skill, training, facilities, organization and a tradition of excellence in manufacture that cannot be acquired over night. The seventy-year old institution back of *Electric Weld Products* guarantees their quality.

Wyatt Metal & Boiler Works maintains plants at both Houston and Dallas, Texas for the manufacturing and fabricating of steel plate products of welded construction. The capacity of both plants is approximately 4000 tons per month, and all work is built to customers' specifications. Plants are equipped to handle heavy plate work completely shop fabricated or field erected. Engineers thoroughly conversant with all phases of design are at the disposal of anyone needing this service.

***York Ice Machinery Corporation, York, Pa.** This company builds a complete line of industrial and commercial refrigerating equipment; year 'round air conditioning systems for commercial, industrial and domestic use; dairy and ice cream machinery; ammonia and Freon valves, fittings and accessories. These products include a great variety of pressure vessels and piping in the fabrication of which welding is employed extensively. Branch sales offices or distributors in all principal cities.

***Youngstown Sheet & Tube Co., Youngstown, Ohio.** Manufacturers of carbon and alloy steel products as follows: bloom slabs, billets, sheet bar, skelp lap weld, butt weld, seamless and electric welded pipe, square and rectangular tubing, mechanical tubing, hot and cold rolled strip steel sheets, auto body sheets, galv-annealed sheets, steel roofing, tin plate, tin mill black plate, steel plate, railroad tie plate and track spikes, merchant bars and structural wire and wire products, wire nails, rigid steel, electrical-conduit, electrical metallic tubing.

Section Activities

ATLANTA, GA.

Temporary Officers:

Chairman—M. C. Snead, Link Belt Company, 1116 Murphy Avenue, S. W.

Secretary—R. G. Wilson, Compressed Ind. Gases.

An organization meeting was held on June 15th. Guy Howard Williams, traveling welding instructor for General Fire Extinguisher Co. made a short address and plea for careful instruction of welders giving experience of his work. Professor Harrelson described at length the welding courses and results at Georgia Tech. and made strong plea for Atlanta Section and offered complete cooperation.

The second meeting was held on Nov. 9th at the Henry Grady Hotel. Chester T. Raymo of the Chattanooga Boiler and Tank Co. was the speaker.

The election of Officers of the Atlanta Section will be held at the December meeting.

BIRMINGHAM

At a meeting held on September 15th at the Tutwiler Hotel the following were elected as Officers of the Birmingham Section:

Chairman—E. E. Michaels, Chicago Bridge & Iron Co., P. O. Box 277.

Vice-Chairman—J. T. MacKenzie, American Cast Iron Pipe Co., Birmingham.

Secretary-Treasurer— J. E. Durstine, Lincoln Elec. Co., 505 No. 22nd st.

Executive Committee—2 year term:

R. E. Jones	W. P. Roberts
R. H. Ledbetter	A. J. Beldon
J. W. Adams	J. P. Flood

1 year term:

E. J. Johnson	J. B. Thompson
W. P. Rierson	I. T. Lindsey
F. E. McAtee	R. D. Keller

The Section had the very good fortune of having the National meeting of the International Acetylene Association in Birmingham, for the November meeting.

Sessions were well attended by a high percentage of the Welding Society members, and due to the Convention the membership of the Birmingham Section will be increased quite materially because of the great interest stirred up in welding.

BOSTON

The following officers of the Boston Section have been elected to serve for the year 1937-38:

Chairman—P. J. Horgan, General Electric Company

Vice-Chairman— W. L. Warner, Watertown Arsenal

Secretary-Treasurer—P. N. Rugg, Edison Electric Illum. Co. of Boston

Directors (for two years):

P. L. F. Feyling, Whitehead Metal Products Co.

C. A. Rober, Bethlehem Shipbuilding Corp.

H. A. Sweet, Assoc. Factory Mutual Fire Ins. Co.

Trustee:

A. L. Combs, Linde Air Products Company

Director of National Society:

H. N. Ewertz, Austin-Hastings Co.

The program of meetings scheduled for this season, as far as it has been arranged is as follows:

Speaker, Nov. 19, 1937, F. G. Flocke, International Nickel.
Subject, "The Welding of Monel, Nickel, and Nickel-Clad Steel."

Speaker, Dec. 27, 1937, R. F. Hellkamp, Air Reduction Sales Co.
Subject, "Gas Cutting."

Speaker, Jan. 21, 1938, Rufus Briggs, Thomson Gibb Electric Welding Company.
Subject, "Resistance Welding."

Speaker, Feb. 5, 1938, W. L. Warner, Watertown Arsenal.
Subject, "Zones Adjacent to Welds."

Speaker, Milton Male, U. S. Steel Corp.
Subject, "Weldability of New Alloy Steel Plate."

Speaker, March, 1938, Roger Clark, General Electric Co.
Subject, "Inspection of Welding."

A review of the meetings of this Section from January 1937 through October 1937, is also of interest, and is given below:

January, N. L. Moekel, Westinghouse Elec. & Mfg. Co.—"Fabrication of 200-in. Telescope for the Palomer Observatory." "Some Westinghouse Welding Experiences."

February, W. D. Halsey, Hartford Steam Boiler Inspection & Insurance Co.—"Investigation of Welding from an Insurance Company's Angle."

March, (at Watertown Arsenal), H. R. Bullock, Mass. Institute of Technology—"Bend Tests, Macro Inspection and other Important Tests."
J. T. Norton, M. I. T.—"Radiographic Inspection."
H. C. Mann, Watertown Arsenal—"Tension and Impact Testing."

April, Joint with American Society of Mechanical Engineers and American Institute of Mining and Metallurgical Engineers cooperating with the N. E. Regional Foundry Conference sponsored by the N. E. Foundrymen's Association and the American Foundrymen's Association—2 days.
A. N. Kugler, Air Reduction Sales Co.—"Multilayer Airco Welding."

October, C. G. Suits, General Electric Co.—"Properties of Air Arc."
E. D. Debes, Bethlehem Shipbuilding Corp.—"The Selection and Qualification of the Men Who Weld."

The attendance for the meetings from January through October totalled about 760.

CANTON, OHIO (Akron, Alliance, etc.)

Temporary Officers:

Robert M. Wallace, The Griscom-Russell Company, Massillon, Ohio.

George M. Gusset, Gusset Boiler Works, Canton, Ohio.

An Open Forum on Welding Problems was held on September 15th at the Canton Y. M. C. A., at which Mr. A. E. Gibson, President, The Wellman Engineering Co., spoke on "Welding of Low Alloy Steels."

The election of officers will be held in December or early January.

CHICAGO

The Officers for 1937–38 of the Chicago Section are as follows:

Chairman—Tom Jones, Carnegie-Illinois Steel Co.

Vice-Chairman—Homer E. Gannett, C. B. & Q. R. R.

Secretary-Treas.— F. L. Spangler, *The Welding Engineer.*

Directors for Three Years—Frank H. Carpenter, Federal Pipe & Supply Co.; I. J. Eales, Steel Sales Corp.; Robert Malcolm, Chicago Eye Shield Co.; Albert Reichman, American Bridge Co.

Director on Board of Directors—C. J. McGregor, American Steel & Wire Co.

The following officers remain in office until the expiration of their terms:

Directors Whose Term Expires in One Year—Gale Adams, Goodman Manufacturing Company; R. G. Mason, A. T. & S. F. Railroad; R. R. McFarland, Western Electric Co., E. L. Quinn, American Manganese Steel Co.

Directors Whose Term Expires in Two Years—J. Kinsock, Standard Oil Co. of Indiana; Leslie McPhee, Whiting Corp.; G. E. Tenney, The Lincoln Electric Co.; D. C. Wright, Sears, Roebuck & Co.

A joint meeting of the Chicago Section and the Maintenance-of-Way Club of Chicago was held on December 14th at the Auditorium Hotel. "Welding and Heat-Treating Rail Ends—By the Gas Process," was presented by Armstrong Chinn, of the Chicago & Alton Railroad, Chicago; By the Electric Process, by C. B. Brownson, of the New York Central Lines, New York City. "Welding Open-Hearth and Manganese Frogs" was presented by S. E. Tracy of the C. B. & Q. Railroad, Chicago.

An Open Forum on "Welding Non-Ferrous Metals" was held on January 15th at the Armour Institute of Technology. Mr. W. C. Swift, of the American Brass Co., spoke on "Copper and Its Alloys;" Mr. F. G. Flocke, of the International Nickel Company, spoke on "Nickel and Its Alloys;" and Messrs. G. O. Hoglund and W. M. Rodgerson of the Aluminum Company of America, covered the subject "Aluminum and Its Alloys." Dinner at 6:15 preceded the meeting. At 7:15 sound movies on several sporting subjects ("Thrills and Spills, Etc.") were shown as an added feature.

On February 19th "Factors Affecting the Properties of Welds and the Base Metal Adjacent to the Weld" was discussed by Leon C. Bibber, Welding Engineer, U. S. Steel Corp., and R. E. Long, Research Engineer, Hollup Corporation. In addition to this, Mr. Bibber discussed the important work being done by the Society on Nomenclature, Definitions and Symbols. A motion picture "Stepping Stones of the Pacific" was shown, preceding the regular meeting.

On March 19th, Mr. H. J. Shiffli, Welding Engineer, American Steel Foundries, spoke on "The Use of Castings in the Assembly of Heavy Welded Sections;" and Mr. J. J. Gordon, Welding Fabrication Division, Taylor-Winfield Corp., spoke on "Assembly of Heavy Sections by Welding," with motion pictures showing production of heavy equipment.

On April 16th, President A. E. Gibson gave a message regarding the AMERICAN WELDING SOCIETY and its activities of interest to all those engaged in the advancement of welding. Mr. George Fred Keck, an Architect, designer of the Crystal House and the House of Tomorrow, at Chicago's World's Fair, spoke on "What Has Welding to Offer the Architect?" Mr. A. S. McCawley of Thomas J. Douglass & Co., spoke on "Welding in the Construction of Building Piping Services."

"New Developments in Welding" was the subject of the meeting held on May 21st at the Mission Hall, Armour Institute of Tech. Mr. T. C. Fockler, Westinghouse Elec. & Mfg. Company presented an address on "Welded Machine Designs" and Mr. A. N. Kugler of the Air Reduction Sales Co., spoke on "Multi-Layer Oxyacetylene Pipe Welding." Mr. H. C. Boardman of the Chicago Bridge & Iron Co., read the English translation of a German paper on Krupp's new welding method, translation of which was furnished by the Fundamental Research Committee.

The first meeting of the Fall Season of the Chicago Section was held on September 17th, at the Skytop Restaurant, Medical and Dental Arts Building. The meeting was preceded by a dinner with an attendance of 54 at the dinner and a total attendance at the meeting of 106. Tom Jones, Chairman of the Section, presided at the meeting, and was followed in order by Warner S. Hays, Managing Director of the AMERICAN WELDING SOCIETY, and Mr. Leon C. Bibber, of the Carnegie-Illinois Steel Company. The meeting was devoted to a Symposium on the new Welding Symbols.

The time of the monthly meetings was decided upon as the third Friday of the month up to and including the May meeting, which will be the last of the season. For location of meetings it was decided for the present to continue at the Skytop Roof located on the top floor of the Medical and Dental Arts Building, 185 N. Wabash Avenue (Corner of Lake St.).

The following is the tentative program for the coming season:

December 17th—Subject "High Pressure—High Temperature Piping."

January 21, 1938—Joint meeting with Western Society of Engineers.

February 18th—Paper by C. H. Jennings, Westinghouse E. & M. Co.

March 18th—Heat Effect in Welding—Dr. W. G. Theisinger, Lukens Steel.

May 20th—Codes and Education in Welding.

The November meeting was held on the 19th in the Skytop Room, 185 N. Wabash Ave., preceded by a dinner. Mr. E. Smith of the Haynes-Stellite Co., spoke on "Hard-Facing in Industry," which was illustrated by two reels of motion pictures.

Mr. A. M. Unger, Welding Engr., Pullman Standard Car & Mfg. Co., reported on the recent convention of the AMERICAN WELDING SOCIETY held in Atlantic City.

CINCINNATI

Officers:

Chairman—W. W. Petry, Cincinnati Milling Machine Co.

Vice-Chairman—F. H. Smith, The Lincoln Electric Co.

Secretary—J. K. Ross, United Welding Co.

Treasurer—C. D. Breese, The Breese Bros. Co.

In response to demand of members and of a representative number of other executives interested in welding in the Cincinnati area, a Chapter of this Society was formed on May 7th.

An inspection trip to the Union Gas & Electric Co., was held on May 20th, This was a joint meeting with the Engineers' Club.

On Sept. 29th, E. W. P. Smith of the Lincoln Electric Co. gave a spectacular screen demonstration of the arc in action. On Dec. 8th, Mr. A. E. Gibson, Wellman Engineering Co., will talk on Low Alloy Steel Welding.

CLEVELAND

The following are the officers elected for the Cleveland Section for the coming year:

Chairman—E. R. Benedict, Engineer, Contract Welders, Inc., Cleveland.

Vice-Chairman—Fred L. Plummer, Assoc. Prof. of Structural Engineering, Case School of Applied Science, Cleveland.

Secretary-Treas.—E. T. Scott, President, Cleveland School of Welding, Inc., Cleveland.

Representative on Board—E. R. Benedict.

Programs held during the 1936–37 season:

November 11th, A. B. Eining, General Manager of The Motch & Merryweather Company presented a talk on "Industrial Conditions in Russia." Leon C. Bibber, Welding Engineer, Carnegie-Illinois Steel Corporation, Pittsburgh, gave an illustrated address on "Some Factors of the Properties of Weld Metal."

R. P. Tarbell, Chairman, gave announcements for the year and E. R. Benedict, Chairman of the Membership Committee gave his report.

December 11th, Fred L. Plummer, Structural Engineer, Case School of Applied Science presented "Model Aids in Welding Design."

January 13th, John D. Gordon, Manager of Fabricating Division, Taylor-Winfield Corporation of Detroit presented "Why Use Fabricated Steels?"

March 10th, Harold O. Jones, Supervisor of Applied Engineering Department, Air Reduction Sales Company

presented "Flame Hardening." Albert Williams, Supervisor of Welding Department, Wellman Engineering Company, presented "Flame Cutting."

April 14th, T. C. Fockler, Industrial Motor Engineering Department, Westinghouse Electric & Manufacturing Company, Pittsburgh, presented "Welded Machine Design."

These Meetings were all very well attended averaging approximately 150 per meeting.

Our November meeting was held at The Wellman Engineering Company plant with an attendance of approximately 150. Mr. A. E. Gibson talked on "Design and Fabrication of Welded Mechanical Equipment" and after the address the members were taken on a tour of inspection through the machine shop and welding department. The meeting was very interesting and we feel it was a good start on our new season.

The following is the tentative program for the balance of this season:

December, Rob't M. Wallace, Assistant Superintendent of The Griscom-Russell Company of Massillon, Ohio will speak on "Problems that Arise in Welding Pressure Vessels."

January, F. G. Flocke, of The International Nickel Company of New York. This is tentative and subject is to be announced later.

February, Speaker from The Crane Company.

March and April, Open

The Cleveland Section co-operates with the Cleveland Engineering Society and the meetings are scheduled for their meeting rooms whenever possible.

CONNECTICUT

Organization meeting held on November 30th. The following officers were elected:

Chairman—E. R. Fish, Chief Engr., Boiler Div., Hartford Steam Boiler Insp. & Ins. Co.

Vice-Chairman—I. T. Hook, Chief Exp. Eng., American Brass Co.

Secretary—H. A. Pennington, Sales Eng., A. B. King & Co.

Treasurer—J. Mitchell, Foreman, Farrel Birmingham Co.

Chairman Program Com.—J. S. Miller, N. Y. N. H. & H. R. R.

Chairman Membership Com.—Marselis Powell, Whitlock Coil Pipe Co.

DETROIT

The following officers of the Detroit Section were elected for the year 1937–1938.

Chairman—Vaughan Reid

Vice-Chairman—D. H. Corey

Secretary-Treasurer—H. P. Doud

Executive Committee, 1937–1939—J. D. Tebben, F. E. Foote

The other two members of the Executive Committee, whose terms expire in 1938, are Walter Anderson and Joe Calley.

Meetings—1937

February, Joint meeting with American Society of Metals. Dr. Armand Di Giulio, Metallurgist, The Ford Motor company—"Metallurgical Factors of Welding."

March, Mr. T. C. Fockler, Westinghouse Electric & Manufacturing Company—"Welded Machine Designs."

April, Memorial program honoring the late Professor Elihu Thomason. Speaker. Mr. A. E. Hackett, Thomson-Gibb Electric Welding Company.

May, Joint meeting with American Society of Mechanical Engineers. Mr. Everett Chapman, President, Lukenweld, Inc.—"Welded Steel in High-Speed Railway Service." Mr. C. L. Eksergian, Chief Engineer, Budd Wheel Company—"Hydromatic Welding of Frames."

October, Mr. John D. Gordon, General Manager, Fabricating Division, Taylor-Winfield Corp.—"Rolled Steel Construction in Machine Design."

November, Mr. Verge Whitmer, Metallurgist, Republic Steel Corporation.—"Fabrication and Welding of Stainless Steels."

December, Joint meeting with Society of Automotive Engineers, at Flint, Michigan. Dr. C. A. Adams, Consulting Engineer, Edw. G. Budd Mfg. Company.—"Fundamentals of Welding." Mr. J. S. Williams, Chief Engineer, P. R. Mallory & Company, Inc.—"Recent Developments in Resistance Welding."

Plans are well under way for programs for the balance of the 1937–38 season.

Membership

The Section increased its membership during the past season from 45 to 105.

Automotive Council

On May 19, 1937, an Automotive Welding Council was formed, composed chiefly of men in the automotive industry. Its purpose is the promotion of information relative to resistance welding, which is used to such a great extent in the automotive industry.

Engineering Society of Detroit

During the past year, the Engineering Society of Detroit formed the "Affiliate Council of The Engineering Society of Detroit." This Council, composed of two representatives chosen from and by each Affiliate Society Member, was formed to facilitate cooperation, especially on public and professional matters, between the engineers of Detroit as individuals and as societies.

The Detroit Section, AMERICAN WELDING SOCIETY, having fulfilled the requirements established by The Engineering Society of Detroit, was accepted as an Affiliate Society and is represented on the Affiliate Council.

HAWAII

The first regular meeting of the Hawaiian Section of the AMERICAN WELDING SOCIETY was held on May 27th at the Oahu Country Club. It was agreed that the fourth Tuesday of each month be designated as the regular Section meeting night. By-Laws were adopted at the meeting and the following officers elected:

Chairman—E. E. Heacock

Vice-Chairman—G. Hansen

Secretary—R. L. Mullen

Treasurer—J. K. Mardick

Executive Committee for two years—H. Ryan, Geo. M. Ross, C. E. Jensen

Executive Committee for one year—Robt. Plaus, P. Orso, W. E. Smith.

After two preliminary organization meetings during the early part of 1937, an organization meeting was held in May 1937. Since then the section has held a dinner meeting at the Oahu Country Club once each month. Speakers have varied from representatives of the U. S. Navy Yard at Pearl Harbor to a General Electric X-Ray apparatus representative.

Membership has gradually increased until it is now about forty.

Probably the outstanding activity of the local section since the organization, occurred Nov. 16th. The growing of sugar cane and manufacture of sugar is the principal industry in the Hawaiian Islands. In connection with this industry there is an Association of Hawaiian Sugar Technologists, which holds an annual meeting in Honolulu. This Association is made up of Chief Engineers, Mill Superintendents, Agriculturalists and Chemists.

On Nov. 16th a demonstration was held before the Engineering Section of this Association and consisted of making and testing pressure pipe welds. Probably the outstanding feature of the program was the hydraulic test which was given several welds of various types on 8″ pipe to a pressure of 3500 lb. without the failure of any weld. This piece of 8″ pipe was permanently deformed from an outside diameter of $8^{5}/_{8}$″ before the test, to an outside diameter of $9^{1}/_{32}$″ after the test.

Due to a shortage of field labor in the Hawaiian Islands, which has affected not only the sugar plantations but also the pineapple plantations, all harvesting and planting is rapidly becoming more and more mechanized. Welding is really coming into its own in this case.

INDIANAPOLIS

Officers:

Chairman—R. D. Eaglesfield, 357 S. La Salle St.

Secretary—I. L. Williams, P. R. Mallory & Co.

The Indianapolis Chapter of the American Society for Metals held a joint meeting with the AMERICAN WELDING SOCIETY on September 27th at the Hoosier Athletic Club. Mr. A. E. Gibson, President, The Wellman Engineering Company addressed the meeting on "The Welding of Low-Alloy Steels."

On Nov. 18th The Linde Air Products Co. showed a motion picture "Automatic Gas Cutting."

The "charter" membership role will be completed at a meeting to be held during the Purdue Welding Conference Dec. 9th and 10th and the new members not listed in this issue will appear in the January issue.

KANSAS CITY

Chairman—C. E. Woodman

Vice-Chairman—J. A. Hall

Treasurer—John Colver

Secretary—Albert W. Roth

The following members were elected to serve on the Executive Committee, representing the consumers for one year: LaMotte Grover, Prof. A. H. Sluss and Walter H. Kurtz.

For two years: N. R. Sack, L. L. Cramer and C. W. Frick.

For one year representatives of The Linde Air Products Co., Puritan Compressed Gas Co., and Harnischfeger Corp.

For two years: Air Reduction Sales Co., Lincoln Electric and General Electric Co.

The Kansas City Section of the AMERICAN WELDING SOCIETY, a new section formed the early part of this year, held during the course of this year, the following meetings:

March 1, 1937—This was an organization meeting. Officers and members of the Executive Committee were elected and By-Laws were approved.

March 13, 1937—This was in the form of a banquet at which Mr. A. E. Gibson, President of the Society, was the speaker and inaugurated the Kansas City Section as a full-fledged section of the Society.

April 26, 1937—The meeting was devoted to "Field Welded Tanks," the speaker being Mr. Harry C. Boardman, Director of Research, Chicago Bridge and Iron Co., Chicago, Illinois.

May 17, 1937—The topic was "Some Factors to be Considered in the Choice of a Welding Process," the lecture being conducted by Mr. Louis J. Larson, Research Engineer in Charge of Welding Research, the A. O. Smith Corporation, Milwaukee, Wisconsin.

June 21, 1937—The lecture was on "Welding Standards" conducted by Mr. Leon C. Bibber, Welding Engineer, Carnegie-Illinois Steel Corp., Pittsburgh, Pennsylvania.

July 19, 1937—Several topics were discussed in a Round Table discussion, including "Welding of Pressure Tanks for Liquified Petroleum Gases," conducted by Mr. Ed. H. Gill, Columbian Steel Tank Company, Kansas City, Mo.; "Gas Cutting as a Means of Preparing Material for Welding," conducted by Victor McDonald, K. C. Structural Steel Co., Kansas City, Mo.; "Maintenance and Repair of Machinery by Welding," conducted by Charles Camp of Camp and Luthy, Kansas City, Missouri.

September 27, 1937—A lecture on "Fabrication of Structural Members and Machine Parts" was conducted by Mr. Frank C. Hutchison, Manager of Process Service, Linde Air Products Co.

October 25, 1937—A lecture on "Comparison of Electric Welding and Brazing High Carbon Wire for Wire Rope" was conducted by Mr. Walter Voigtlander, Chief Engineer of Union Wire Rope Company, Kansas City, Mo.

November 15, 1937—A lecture and demonstration of "Metal Spray," conducted by J. G. Magrath of Air Reduction Sales Co.

Meetings scheduled for the balance of the season are as follows:

December Meeting—Speaker, Mr. A. E. Gibson.

January Meeting—Speaker, Mr. Fantz of Mid-West Pipe and Supply Company of St. Louis.

February Meeting—Speaker, Mr. G. Raymond of Black, Sivalls and Bryson of Oklahoma City.

The Kansas City Section is cooperating with Kansas University of Lawrence, Kansas in working out the plans for the Annual Welding Meeting held at the University. This meeting will be held the second or third week of March 1938, at Lawrence, Kansas, the University contributing to the program papers, the result of research recently carried on.

Due to conflict with other local activities, this Section cannot always be assured of a permanent meeting place. Therefore, meetings are either held in the Municipal Auditorium or the Kansas City Power and Light Building. Meetings are scheduled for the third Monday of each month but sometimes, due to necessary arrangements for out-of-town speakers, this is naturally subject to change. For full information relative to meetings, out-of-town guests may obtain this information from either the Chairman or the Secretary of this Section.

The Kansas City Section has grown from less than ten members the first part of this year at the time plans were being laid for a Section, to a membership of over one hundred. A most active campaign is being conducted by our Membership Committee to assure a steady growth of the number in our Section.

LOS ANGELES

Officers:

Chairman—Wayne A. Howard, General Petroleum Corp.

Vice-Chairman—P. D. McElfish, Standard Oil Co. of Calif.

Secretary-Treasurer—J. C. Gowing, P.O. Box 186, Huntington Park.

Executive Committee:

J. C. Blake, Victor Equipment Co.

L. R. Earl, Consolidated Steel Corp.

J. C. Gowing, J. C. Gowing Co.

C. A. Mott, National Cylinder Gas Co.

H. C. Whittlesey, Consulting Structural Engr.

The regular meeting of the Los Angeles Section was held February 18th at the Los Angeles Chamber of Commerce Building with 85 members and guests present. The meeting was preceded by a dinner at 6:30.

The first speaker, Mr. B. J. Brugge, recently Welding Engineer for the Anglo-Persian Oil Co. of Abadan, Persia and at present Sales Engineer for The Lincoln Electric Co., analyzed the material presented in the February issue of THE WELDING JOURNAL.

The second speaker, Mr. Arthur Beggs, Western Sales Manager of the American Airlines, Inc., gave an interesting talk on "Modern Air Transportation" and took his listeners on an imaginary trip to New York on one of the new sleeper planes.

The third speaker, Mr. Kenneth Anderson, Pacific Coast Representative of Steel and Tubes, Inc., spoke on "The Manufacture of Welded Tubes and Pipe by the Continuous Electrical Resistance Process." His talk was illustrated by motion pictures showing manufacturing processes. Numerous specimens of tubing and tubing tests were presented for inspection.

The fourth speaker was Mr. Charles Babbitt, Welding Foreman, Western Pipe and Steel Co., who showed and described a motion picture of the welding arc.

March 18th a meeting was held at the Central Manufacturing District Club Rooms with 90 members and guests present.

Mr. Zeno Klinker, humorous writer and aviation enthusiast, presented "Man's Conquest of the Air." This is a motion picture which is the most complete and comprehensive collection of authentic historical aviation films in the world.

Mr. Bruce Burns, Consulting Aircraft Engineer, then spoke on "Aircraft Spot Welding of Aluminum Alloys and Corrosion-Resisting Steels."

The third speaker was Mr. B. J. Brugge, who presented a review of the current issue of THE WELDING JOURNAL.

Mr. Turner Smith, Regional Vice-President, then lead a discussion of the AMERICAN WELDING SOCIETY'S Code for the Qualification of Welding Processes and of Welding Operators. This topic was discussed in an attempt to offer the Section's recommendation to the national organization.

At the April 15th meeting 50 members and guests were present.

Dr. Gordon N. Scott, Pipeline Technologist, formerly American Petroleum Institute Associate at the National Bureau of Standards in Washington, spoke on "Cathodic Protection and Corrosion of Pipe Lines."

A sound motion picture, "The Manufacture of Alloy Steels" was next. This was presented through the courtesy of the Bethlehem Steel Co. and Mr. George Stetter, Metallurgist of the Los Angeles plant.

The current issue of THE WELDING JOURNAL was reviewed by Mr. J. C. Blake, Manager of the Victor Welding Equipment Co.

The regular meeting was held May 20th at the Central Manufacturing District Club Rooms with 115 members and guests present.

The topic for the evening was "Building the World's Largest Telescope."

The first speaker, Dr. John A. Anderson of the California Institute of Technology, discussed, in the language of the layman, the optical theory of this huge instrument. He also described the many problems which were encountered in casting, grinding and polishing the 200-inch mirror.

The second speaker was Mr. Mark Serrurier, Structural Engineer and designer of the supporting structure. Mr. Serrurier gave a word picture of the research necessary to develop a suitable structural steel mounting for the world's largest telescope.

On June 10th, Mr. George A. Maurath of Maurath, Inc., talked on "Welding of Stainless Alloys."

The possibility of another Educational Course next year, similar to the one presented this spring, was discussed and the committee felt that the success of the past course justified presenting another course next year.

Due to transfer of Chester T. Mott, Membership Committee Chairman, to the East, Mr. P. D. McElfish was appointed to that position.

Russell Graves, Secretary and Treasurer, is being transferred to Oleum, California, and J. C. Gowing was appointed as Secretary-Treasurer for the remainder of the term.

The matter of programs for the regular meetings was discussed and it was decided to proceed as in the past, that is, to present one technical paper and one general paper at each meeting. The Committee also instructed the Secretary to have notices of regular meetings mailed to all Pacific Coast Sections of the different Societies.

The first meeting after the summer vacation was held on September 22, 1937, in the club rooms of the Central Manufacturing District Club.

Ninety one dinner tickets were sold and the attendance for the meeting following was approximately 130 members and guests.

A very interesting talking picture on Stainless Steels was presented by Frank Rider of the Republic Steel Corp.

Mr. E. Marksheffell, Superintendent of the Los Angeles Terminal, of the Union Pacific Railroad, talked on Modern Transportation, describing the Streamliners and welding as it applied to the railroads of today.

W. J. Bruggee reviewed the JOURNAL for the past month.

MARYLAND

Officers.

Chairman—Dr. J. W. Miller, Reid Avery Co.

Vice-Chairman—C. M. Underwood, U. S. Navy Yard, Washington, D. C.

Secretary-Treas.—C. N. Hilbinger, The Lincoln Electric Co.

Members Executive Committee, 1937–40:

Albert W. Foster, Foster's Welding Shop.

James E. Taylor, Black & Decker Co.

A complete list of the meetings held during the year 1937.

January 1937, T. R. Lichtenwalter, Republic Steel, Motion Picture—"Bringing the Steel Mill to You."

February 1937, J. F. Lincoln, president of Lincoln Electric Co.—"Welding *vs.* Prejudice."

March 1937, W. C. Stewart, U. S. Naval Experiment Station—"Sprayed Metal Coating, Properties and Applications."

April 1937, Everett Chapman, Luken Weld, Inc.—"Control of Deflections in Welding."

October 1937, Second Annual Oyster Roast, or get-together.

November 1937, Harry W. Pierce, New York Shipbuilding Corp.—"Welding in Shipbuilding."

Our program schedule for the first six months of 1938 is:

January 21, 1938, Arthur M. Kugler, Air Reduction Sales Co.—"Multiple Pass Gas Welding."

February 18, 1938, T. W. Greene, Linde Air Products Co.—"Testing Welds."

March 8, 1938, Joint Meeting with A. S. M. E., C. H. Jennings, Westinghouse Electric & Mfg. Co.—"Welding Design."

April 8, 1938, A. F. Davis, Lincoln Electric Co.—"The Human Side of Welding."

In the early part of 1937, a committee was appointed from the Maryland Section to work with the Baltimore City Engineers on the revision of their Building Code. The Committee reports satisfactory progress has been made and they feel certain that when the new Building Code is completed, which will be within the next six months or a year, welding will be permitted on the same.

The following is a brief report on the last two meetings:

October 30, 1937 the Maryland Section held their Second Annual Oyster Roast. About 85 members and friends were present and an enjoyable time was had by all.

On November 19, 1937, Mr. Harry Pierce of the New York Shipbuilding Corp. spoke before 75 members and friends of the Maryland Section on "Welding in Shipbuilding." After the address the meeting was thrown open to discussion.

MILWAUKEE

Chairman—K. L. Hansen, Harnischfeger Corp.

Vice-Chairman—H. F. Falk, Falk Corporation Secretary-Treasurer—J. J. Chyle, A. O. Smith Corp.

Directors (two years):

Fred O. Volz, Lakeside Bridge & Steel Co.

J. G. Shodron, Consulting Engineer.

S. H. Smith, Air Reduction Sales Co.

Director (one year) to fill unexpired term of late Mr. Kaiser of The Linde Air Products Co.

G. F. Meyer, Machinery Welder Corp.

The regular monthly meeting of the Milwaukee Section was held on January 21st at the Milwaukee School of Engineering. Mr. Harold Verson, Chief Engineer of the Allsteel Press Company, spoke on "Problems of Welding in Machine Tools."

A technical meeting of the Milwaukee Section was held on Friday, Feb. 26th, at the City Club of Milwaukee.

Mr. J. C. Joublanc, Chief Metallurgist, Harnischfeger Corp., presented an address on "Some Metallurgical Aspects of Arc Welding." In addition to the subject matter, Mr. Joublanc discussed the effects of stress-relieving and normalizing on welded structures.

A joint meeting of the Milwaukee Section of the AMERICAN WELDING SOCIETY with the American Society of Civil Engineers was held on Tuesday, March 30th at the City Club of Milwaukee. The speaker of the evening was Mr. A. M. Candy, Consulting Engineer, Hollup Corp., who spoke on "Arc Welding in Structural Engineering."

On May 27th, "Recent Developments in Machine Cutting with the Oxyacetylene Flame" was presented by Mr. Gus Johnson, Welding Engineer, Air Reduction Sales Company; and "Hard Facing" was presented with illustrations by moving pictures by Mr. E. F. Smith, District Manager, Haynes Stellite Co.

The opening meeting of the 1938 season was held on Friday, October 15th at the City Club of Milwaukee. Dinner preceded the Technical Meeting. Mr. W. V. Emery, Chief Welding Inspector of the Harnischfeger Corp., presented a talk on "Shop Organization and Practical Problems in Welding." Following the talk, several well known shop executives gave their views on the subject.

A technical Meeting was held on Nov. 10th at the City Club of Milwaukee. Mr. Karl T. Nystrom, Production Engr., Milwaukee Road Shop, presented an illustrated talk on "Welding of Streamlined Railway Cars."

MONTANA

Chairman—Henry F. C. Rumfelt, Fort Peck.

Vice-Chairman—Adolph F. Chaw, Nashua, Montana.

Secretary-Treasurer—Allen W. Bates, Fort Peck.

Section Representative—W. C. Anderson, Fort Peck.

At a meeting on January 6th of the Montana Section, Mr. Wm. M. Galloway of the U. S. Engineers presented a historical paper on "Welding on the Fort Peck Dam Project." Following the presentation of Mr. Galloway's paper, an open discussion was held of an article on Thermit Welding appearing in the December issue of THE WELDING JOURNAL.

At the February 3rd meeting a round table discussion was held on Welding Procedures. Some very interesting questions about local problems were asked and answered by those present. Considerable time was devoted to a discussion of Corrosion of Welds.

At the March 3rd meeting of the Montana Section a talk was given by Mr. Roy L. Lerch, Sales Manager of the Haynes Stellite Company, entitled "Hard Surfacing Wearing Parts." Included in Mr. Lerch's talk was a paper by E. E. LeVan, entitled "Hard Facing."

At the April 7th meeting two papers were read "Welding as Employed in the Fort Peck Tunnels," by V. H. Byrnes, and "The Relation of the Engineer to the Welder and Foundry Man," by T. B. Jefferson.

The Montana Section held their regular meeting June 2nd in the Administration Building at Fort Peck. A paper entitled "Arc Welding Power Supply," was presented by Roy L. Spaulding, Junior Engineer. This paper was followed by Mr. Peterson of the Government Laboratory who exhibited with the aid of a high power microscope, and microphotos, a piece of 0.90 high carbon steel that had been welded.

NEW YORK

Officers:

Chairman—R. W. Boggs, The Linde Air Products Co.

1st Vice-Chairman—J. L. Edwards, H. G. Balcom & Associates.

2nd Vice-Chairman—E. Tangerman, Managing Editor, Power.

Secretary-Treasurer—Milton Male, U. S. Steel Corp.

National Director—S. S. Scott, U. S. Navy.

Directors—3 years:

C. Kandel, Craftsweld Equipment Co.

H. H. Moss, The Linde Air Products Co.

J. L. Wilson, American Bureau of Shipping.

C. W. Bryan, Jr., Federal Shipbuilding & Dry Dock Co.

Directors—2 years:

G. D. Fish, Consulting Engineer.

R. K. Hopkins, M. W. Kellogg Co.

J. B. Tinnon, Metal & Thermit Corp.

J. W. Sheffer, American Car & Foundry Co.

Directors—1 year:

S. S. Scott, U. S. Navy.

E. Vom Steeg, Jr., General Electric Co.

G. N. Bull, The Lincoln Electric Co.

W. Lonsdale, Foster-Wheeler Co.

The New York Section has scheduled eight meetings for the year 1937–38 as follows:

September—Talk on welding of organic materials.

November—Joint meeting with A. S.-C. E. Subjects—"Developments in Flame Cutting High Strength Structural Steels" and a motion picture construction of San Francisco–Oakland Bay Bridge.

December—Automatic Welding in Shipbuilding.

January—Joint meeting with A. S. M. E. Subject—"Jigs and Fixtures."

February—Structural Welding.

March—Safety.

April—Ferrous and Non-Ferrous Alloys.

May—Business Meeting and Smoker.

Following its successful lecture course given last year, for which over 300 registered, a second course, of somewhat more advanced subject matter will be given this year. Starting in January, the course is planned in cooperation with the Brooklyn Polytechnic Institute.

The New York Section, because of its close affiliation with headquarters, has always cooperated, and will continue to do so, in any worthy welding activity. During past years, many of its members have been active in furthering the adoption by the City of New York of provisions for welding in the Building Code, and, following its recent adoption, stand ready to be of assistance in any applications which may result.

NORTHERN NEW YORK

Chairman—W. F. Hess.

1st Vice-Chairman—F. H. Miller.

2nd Vice-Chairman—R. W. Clark.

Secretary-Treasurer—G. A. Ross.

Director at Large—L. R. Leveen.

Directors—R. T. Gillette, A. G. Cochrane (Terms ending May 31, 1938), M. Unger, R. L. Browne (Terms ending May 31, 1939), E. J. Myers, T. R. Lawson (Terms ending May 31,1940).

Membership Committee: Chairmen—R. W. Clark—Schenectady Area and E. F. Potter—Pittsfield Area, F. D Fallon, R. A. Gilbert, E. R. Spittler, J. Ransom, W. E. Wilson.

Meetings and Papers Committee: Chairman—W. C. Hutchins, M. Unger, F. H. Miller, C. Durham, W. Rash, W. C. Reed.

Public Relations Committee: L. D. Meeker, L. R. Leveen, J. E. Waugh.

Engineering Coordinating Committee: W. C. Hutchins.

Meetings Held During 1936–37

Sept. 24th—Welding Demonstration and Exhibit at Schenectady.

Oct. 29th—Dinner meeting. Discussion of papers given at Cleveland. New Kenmore—Albany.

Dec. 10th—Dinner meeting R. P. I. Speaker, A. E. Gibson. Also sound pictures on mfg. stainless steel.

Jan. 21st—Sound movies of San Francisco, Oakland and Golden Gate Bridge.

Feb. 25th—Welding of Non-ferrous metals.

Mar. 25th—Demonstration at R. P. I.

April 22nd—Welder's meeting. Discussion of cast iron welding. Gas Welding. Movies.

May 13th—Annual meeting.

Meeting Program for 1937–1938

Sept. 30th—Dinner meeting—Pittsfield, Mass. Procedure Control—Skit—J. E. Waugh and R. W. Clark. Attendance 102.

Oct. 28th—"The Making of Fine Steels"—Moving pictures and inspection tour of Watervliet, N. Y. plant of Ludlum Steel Co. Attendance 102.

Nov. 18th—Dinner meeting—Schenectady, N. Y. "High Lights of Atlantic City Exposition." Discussion of convention papers. Attendance 61.

Dec. 16th—Dinner meeting—R. P. I.—Troy, N. Y. "Flame Hardening" J. H. Zimmerman, The Linde Air Products Co.

Jan. 6th—Meeting of Combined Engineering Societies, Schenectady N. Y.—"Control of Distortion When Welding." N. L. Mochel, Metallurgical Eng., Westinghouse.

Feb. 17th—"Brazing and Soldering"—Leo Edelson of Handy and Harmon and W. C. Reed, G. E. Co., Schenectady, N. Y.

Mar. 17th—Resistance Welding—Schenectady, N. Y.

Apr. 21st—Dinner meeting. R. P. I.—Troy, N. Y. Subj. to be announced.

May 19th—Annual Meeting.

WESTERN NEW YORK

Chairman—H. J. Schleider.

Vice-Chairman—Robert Siemer.

Secretary-Treasurer—F. O. Howard.

Directors (2 years)—J. A. Fish and F. L. Rogers.

Directors (1 year)—Charles S. Freeman and M. P. Korn.

Representative on Board—F. O. Howard, American Steel & Wire Co.

A reorganization meeting, the first held in four years by this Section, took place at the Hotel Statler, Buffalo, Thursday, March 18th at 8 P.M. The program included an introductory address by Col. Martin P. Korn, Consulting Engineer of Buffalo and an address of welcome by A. E. Gibson, President of the Society. The principal address was an illustrated talk by E. W. P. Smith, Consulting Engineer, The Lincoln Electric Company, which included stress distribution in various kinds and types of welded joints by projecting rubber models on a screen, also by projecting celluloid models using polarized light. There were about 150 present.

A very successful meeting of the Western New York Section was held on May 7th at the Hotel Statler, at which there were present about 125 persons. Mr. Leon C. Bibber, Welding Engineer of the Carnegie-Illinois Steel Corp., presented as his subject "Welding Standards," including the AMERICAN WELDING SOCIETY'S new symbols; also Mr. D. E. Roberts, Engineer of The Linde Air Products Co., gave a very interesting talk accompanied by motion pictures of Automatic Flame Cutting.

Meetings scheduled:

Oct. 26th—Prof. S. C. Hollister, Dept. of Engineering, Cornell University—"Boulder Dam."

Nov. 22nd—R. L. Lerch, Haynes Stellite Co.—"Hard Facing."

Jan. 13th—Joint meeting with A. S. M. E. —Dr. Theisinger, Luken Steel Company—"Heat Effect in Welding."

February—General Electric X-Ray.

March—Joint Meeting with Buffalo Engineering Society.

The Section is cooperating with the Buffalo Engineering Society and other engineering societies for the promotion of welding and cutting.

NORTHWEST

Chairman—W. E. Murphy, Manager, Power Sales Department, Northern States Power Company, Minneapolis.

Vice-Chairman—C. M. Akins, Vice-President, Marquette Manufacturing Co., Minneapolis.

Secretary-Treasurer—Alexis Caswell, Secretary, Manufacturers' Association of Minneapolis, Inc.

Executive Committee—J. H. Barron, Plant Manager, Brown Sheet Iron & Steel Co., St. Paul; C. E. Comfort, Superintendent, St. Paul Structural Steel Co., St. Paul; G. W. Irwin, District Manager, Air Reduction Sales Co., Minneapolis; Fred Klass, Sales Agent, General Electric Company, Minneapolis; H. A. Lindeke, Sales Agent, Nicols, Dean & Gregg, St. Paul; John Nelson, Vice-President and Manager, Butler Manufacturing Co., Minneapolis; R. H. Newton, District Manager, Lincoln Electric Company, Minneapolis; R. W. Robinson, Manager, American Bridge Company, Minneapolis; V. L. Sage, District Manager, National Cylinder Gas Co., Minneapolis; G. V. Watson, President, Flour City Welding Co., Minneapolis; J. E. Williams, District Manager, Linde Air Products Co., Minneapolis.

The rapidly increasing use of welding in industrial operations convinced a number of manufacturers and others that it was not only desirable but necessary to organize a section of the American Welding Society in the Northwest. After preliminary investigation and discussion of the benefits of such a section, a meeting of those who might be interested was called by Harry J. Kicherer, plant manager of the American Hoist & Derrick Company, St. Paul, on June 21, 1937, at which plans for organizing a section were thoroughly considered. Howard Chatfield, supervising engineer of the district office of the Fidelty & Casualty Company at Minneapolis, spoke at that meeting on "The Importance of Skill in Welding." Temporary officers and committees were appointed to complete arrangements for the organization of the section.

On July 28, 1937 the organization meet-

ing was held, officers and members of the Executive Committee were elected, the chairmen of the necessary committees appointed, and a constitution and by-laws adopted. L. A. Dills, senior engineer of the State of Minnesota Department of Highways, gave a most interesting talk on "Welders' Qualifications."

Program 1937–38 Season:

Sept. 27th—Harry Boardman, Chief Engineer, Chicago Bridge & Iron Co., Chicago, "Fabrication of Tanks in the Shop and in the Field."

Nov. 4th—W. M. B. Brady, District Arc Welding Specialist, General Electric Co., Chicago, "Modernizing with the Electric Arc."

Jan. 27th—W. B. Keelor, Ingersoll and Disc Steel, Division, Borg-Warner Corporation, Chicago, "Welding of Stainless and Stainless Clad Steels."

Feb. 17th—W. B. Browning, The Linde Air Products Co., Chicago, "Recent Developments in the Use of Oxygen and the Oxy-Acetylene Process."

Mar. 17th—J. D. Gordon, Manager, Fabricating Division, The Taylor-Winfield Corporation, Detroit, Michigan, "Redesign and Fabrication of Simple and Complicated Parts."

Apr. 21st—Eric Seabloom, Research Welding Engineer, Research Testing Laboratories, Crane Co., Chicago, "Pipe Welding."

May 19th—H. O. T. Ridlon, District Manager, The Bastian-Blessing Co., Chicago, "Hard Surfacing."

J. E. Williams, chairman of the Membership Committee, is organizing the membership work of the Section, which undoubtedly will result in a gratifying number of new members. The Section now has 57 members.

Under the leadership of Chairman W. E. Murphy it is believed that the art and science of welding in this territory will be greatly benefited by the activities of the Northwest Section. There will be close cooperation with the American Society for Metals and other similar organizations, which should prove profitable in every way.

OKLAHOMA CITY

The organization meeting of the Oklahoma City Section was held Monday evening, September 27th in the Biltmore Hotel, with an attendance of 31, mostly members. The following officers were elected:

Chairman—O. T. Barnett, Black, Sivalls & Bryson, Box 1377, Oklahoma City.

Vice-Chairman—M. G. Mossholder, Okla. City Machine Works.

Treasurer—Otto Hart, Hart Welding Supply, 409 W. California.

Secretary—K. B. Banks, P. O. Box 1377, Oklahoma City, Okla.

Representative on the Natl. Board of Directors—G. Raymond, 3110 N. W. 20th St.

Member of the Natl. Membership Committee—O. B. Fulton, Hart Welding Supply, 409 W. California.

The meeting opened with Temporary Chairman O. T. Barnett presiding and his opening remarks were followed by a brief history of the organization Section given by the Temporary Secretary K. B. Banks.

G. Raymond gave a few remarks on the outlook and possibility of welding.

M. G. Mossholder gave a brief talk on the welding of cast iron.

The first regular meeting of the Oklahoma City was held on November 2nd. Mr. M. L. Rogers, gave an interesting talk on the methods used in repairing a large cast iron flywheel. Subsequent meetings will be held the first Tuesday of each month.

PHILADELPHIA

Chairman—T. M. Jackson, Sun Shipbuilding & Dry Dock Company, Chester, Pa.

Vice-Chairman—Ed. G. Hosted, 603 Country Club Lane, Manoa, Upper Darby, Pa.

Treasurer—R. D. Thomas, Arcos Corporation, 401 N. Broad St., Phila., Pa.

Secretary—H. E. Hopkins, Arcos Corporation, 401 N. Broad St., Phila., Pa.

Junior Past-Chairman—C. I. MacGuffie, General Electric Co., Mitten Building, Phila., Pa.

Executive Committee—J. W. Allison, Westinghouse Elec. & Mfg. Co., H. R. Salisbury, Air Reduction Sales Co., W. F. Carson,

April 20, 1936—Lieut. Comm. H. N. Wallin, U. S. N., "Some of the Problems which arise in Connection with Welding in Ship Construction." Present—150.

May 18, 1936—Inspection Trip—Edw. G. Budd Mfg. Co. Plant. Present—60.

September 21, 1936—Walter Graf, Engineering Department, Edw. G. Budd Mfg. Co., "Modern Day Welding in the Automotive and Railway Fields." Present—55.

October 26, 1936—Dr. Comfort A. Adams, "Research and the American Welding Society." Present—35.

November 16, 1936—R. K. Hopkins, Director, Metallurgical Research, M. W. Kellogg Co., "Welding in Chemical and Refining Equipment." Walter Samans, Chief Engineer, Atlantic Refining Co., Talk supplementing Mr. Hopkins' paper. Present—60.

January 18, 1937—J. G. Hartley, Engineer, Maintenance of Way Department, Pennsylvania Railroad, "Rail Welding." Present—65.

February 15, 1937—F. G. Flocke, Technical Service Engineer, International Nickel Co., "The Electric Welding of Monel Nickel and Nickel Clad Steel." Present—70.

March 15, 1937—Chas. H. Jennings, Engineer in Charge of Welding Research, Westinghouse Electric & Mfg. Co., "Solving Manufacturing Problems by Welding." Present—35.

April 19, 1937—T. R. Lichtenwalter, Republic Steel Corp., "Manufacture, Uses and Applications of Enduro Stainless Steel." Present—70.

1937–38 program:

October 19, 1937—Hotel Traymore, Atlantic City. Philadelphia Day at the Atlantic City Convention of the American Welding Society. Sessions all day.

November 15, 1937—8:00 P.M., Engineers' Club, Philadelphia. New Developments in Welding—Flame Hardening—Multi-Pass Welding, Dr. G. V. Slottman, and A. N. Kugler, Air Reduction Sales Company.

January 17, 1938—8:00 P.M., Engineers' Club, Philadelphia Electric Welding Progress, J. F. Lincoln, Lincoln Electric Company.

February 21, 1938—8:00 P.M., Engineers' Club, Philadelphia. Fabrication and Uses of High Yield Strength Alloys, U. S. Steel Corporation.

March 21, 1938—8:00 P.M., Engineers' Club, Philadelphia. The Uses of Castings In Heavy Welding Assemblies, Mr. Jessup, S. Morgan Smith Company, of York, Pa.

April 18, 1938—8:00 P.M., Engineers' Club, Philadelphia. Welding In Modern Building Construction, E. L. Durkee, Bethlehem Steel Corporation.

May 1938—INSPECTION TRIP. Another one of our interesting trips through a nearby plant where welding is an important activity.

PITTSBURGH

Chairman—Leon C. Bibber, Welding Engineer, Carnegie-Illinois Steel Corporation.

Vice-Chairman—J. O. Rinek, Vice-President, Universal Steel Corporation.

Secretary—J. F. Minnotte, Secretary-Treasurer, Minnotte Brothers Company.

Representative on National Board of Directors—W. W. Reddie, Motor Sales Division, Westinghouse Electric & Manufacturing Co.

The following meetings were held:

January 27, 1937—"Why We Weld," by A. E. Gibson, President of the AMERICAN WELDING SOCIETY and Vice-President of the Wellman Engineering Company, Cleveland, Ohio. Due to illness of Mr. Gibson, L. C. Bibber presented paper on the above subject.

Lecture—"Industrial Conditions in Russia," by A. B. Einig, General Manager of Motch & Merryweather Company, Cleveland, Ohio.

February 17, 1937—"Why Use Fabricated Steel," by J. D. Gordon, Manager, Taylor-Winfield Corporation, Detroit, Michigan.

March 17, 1937—"Past and Future of Welding in the Pittsburgh District," by G. O. Carter, Consulting Engineer, Linde Air Products Co., New York, N. Y.

April 21, 1937—"Safety in Welding," by R. B. Lincoln, Director National Weld Testing Bureau, a division of Pittsburgh Testing Laboratory, Pittsburgh, Pa.

October 13th—"The Use of Rolled Steel in Welded Construction," by A. E. Gibson, President of the AMERICAN WELDING SOCIETY and President of Wellman Engineering Company, Cleveland, Ohio. "The American Welding Society," by Warner S. Hays, Managing Director, AMERICAN WELDING SOCIETY.

November 17th—"The Welding of Large Spherical, Spheroidal and Other types of Tanks and Structural Work," by H. C. Boardman, Director of Research, Chicago Bridge & Iron Works, Chicago, Illinois.

A "member for member" drive was begun sometime in September under the Chairmanship of A. E. Marble and the

entire district is being canvassed for new members.

The City of Pittsburgh has issued a special permit to allow the structural steel framework for the new Presbyterian Hospital to be welded and at the same time has appointed a committee to consider an amendment to the Building Code to include welding of structures in the city. This is one of the most outstanding results of the activities of the Pittsburgh Section Building Code Committee.

SAN FRANCISCO

Officers:

Chairman—N. F. Ward, Associate Professor, Univ. of California, Berkeley.

Vice-Chairman—T. R. Rooney, Western Pipe and Steel Co., San Francisco.

Secretary—J. G. Bollinger, Air Reduction Sales.

Treasurer—H. Saunders, Air Reduction Sales.

Executive Committee—Officers, and C. S. Smith, Linde Air Products Co., K. V. King, Standard Oil Co., L. L. Mathy, Victor Welding Equipment Co.

Section Representative, AMERICAN WELDING SOCIETY Directors—N. F. Ward, Univ. of California.

September 1936—The opening meeting of the fall season had as main feature of the program the Premiere Showing of a most interesting moving picture film illustrating each state of the Standard Oil Company's operations at the Bahrein Oil Fields in Persia. Mr. K. V. King gave a condensed survey of outstanding articles on welding which recently appeared in the Society JOURNAL, as well as in other contemporary literature. Refreshments were served.

October 1936—Mr. E. Brooker, Engineer of the Standard Oil Company, reviewed the last issue of the AMERICAN WELDING SOCIETY JOURNAL, commenting on various outstanding articles. The speaker of the evening was Dr. Von Plank of the Columbia Steel Co. who spoke on "Low Alloy High Tensile Steel," from view-point of the metallurgical engineer.

November 1936—Mr. K. V. King gave a résumé of the outstanding articles pertaining to the welding process appearing in the JOURNAL, and other contemporary literature. The principal speaker was Prof. N. F. Ward, whose topic was "Review of Recent Developments in Low Alloy Steel Welding," from the view-point of the fabricator.

December—The meeting was in the nature of a pre-Christmas get-together as well as a formal meeting. Mr. L. C. Henderson, District Manager for the Lincoln Electric Co., spoke on the subject "Arc Welding of Low-Alloy High-Tensile Metals." Mr. C. S. Smith, District Manager for The Linde Air Products Co., spoke on "Oxyacetylene Welding of Alloy Steels."

January 1937—Mr. K. V. King continued his interesting survey of important current welding literature with emphasis on the splendid articles appearing in the JOURNAL. The principal speaker was Mr. C. S. Fuller, in charge of Gas Construction and Operation, Pacific Gas & Electric Co., whose subject was "Welding on High Pressure Gas Lines."

February—Mr. Brooker, an Engineer of the Standard Oil Company of California, reviewed several outstanding articles in the last issue of the JOURNAL. The speaker of the evening, Mr. Harold Bell, Service Operator of The Linde Process Service Department, presented an illustrated paper on "Oxyacetylene Cutting and Manufacture and Utilization of Rolled Steel." Two reels of movies on flame cutting were shown through the courtesy of The Linde Air Products Company.

April (2nd)—Mr. W. E. Emmett, District Engineer of the American Structural Steel Association, gave a very interesting talk on "The Welding of Structural Shapes in Buildings and Bridges." Then followed a brief open discussion regarding the qualification of welders. A motion picture on the subject of Hard Surfacing was presented by the Stoody Company and shown through the auspices of the Victor Equipment Co. A résumé on the more interesting articles of the March JOURNAL was presented by Mr. Brooker, Engineer of the Standard Oil Company of California.

April (30th)—The principal speaker of the evening was Lieut. Commander W. McL. Hague, U. S. M., whose subject was "Welding in Ship Construction." The next speaker was Mr. H. A. Storrs, Chief Inspector, East Bay Municipal Utility District, whose subject was "Strain Measurements in Welds."

May—The Annual Meeting of the San Francisco Section was held at the Athens Athletic Club. The major portion of the evening was devoted entirely to a good fellowship get-together. Prizes were awarded for the funniest personal experience in the welding business; the funniest or snappiest story of the evening and the biggest boner ever pulled in the welding business.

August—The San Francisco Section's first meeting of the year 1937–38 was held at the Engineers' Club. Membership plans were announced. This Section also plans to give a prize for the best paper written by students of the various universities within a certain district to be decided upon by a committee of the Section. Mr. K. V. King presented a brief review of the various articles published in recent issues of the JOURNAL. Mr. L. W. Delhi, General Manager of the Western Pipe and Steel Co. of San Francisco, addressed the audience on "Organization Work in Connection with Fabrication of Large Welded Pipe Line Contract."

September—A sound motion picture on the production of Stainless Steel, furnished through the courtesy of the Republic Steel Corporation was shown, followed by a preliminary discussion and a lecture by Mr. R. Waldman, of the Chicago Steel & Wire Co.

October—The meeting was held at the plant of the Western Pipe & Steel Works, in South San Francisco. The program was under the direction of Mr. T. R. Rooney, Chairman of Programs, and included a brief survey of the operations and a visit to the plant.

The most important contribution to the welding industry in this region has been the development of large pipe welding for high pressure gas lines, oil lines and water lines, these pipes being fabricated in the welding shop in two section units and later fabricated in the field.

This Section has been fortunate in having the cooperation of the American Institute of Steel Construction through the District Engineer W. E. Emmett, who has done considerable work in the clarifying structural specifications for the State Departments of Highway, Buildings, and Construction.

This Section has gained respect from the State University, the secondary schools, welders and executives by its promotion of educational methods, its programs outlining the latest developments in the industry. The position of the section has been that of informant for those who wish the correct information on welding. If the Section is unable to provide the answers then competent individuals with experience can be relied upon to furnish the information.

ST. LOUIS

Chairman—A. W. Harris.

Vice-Chairman—F. J. Feldhaus.

Secretary—C. W. Sammelman.

Treasurer—N. F. Moss.

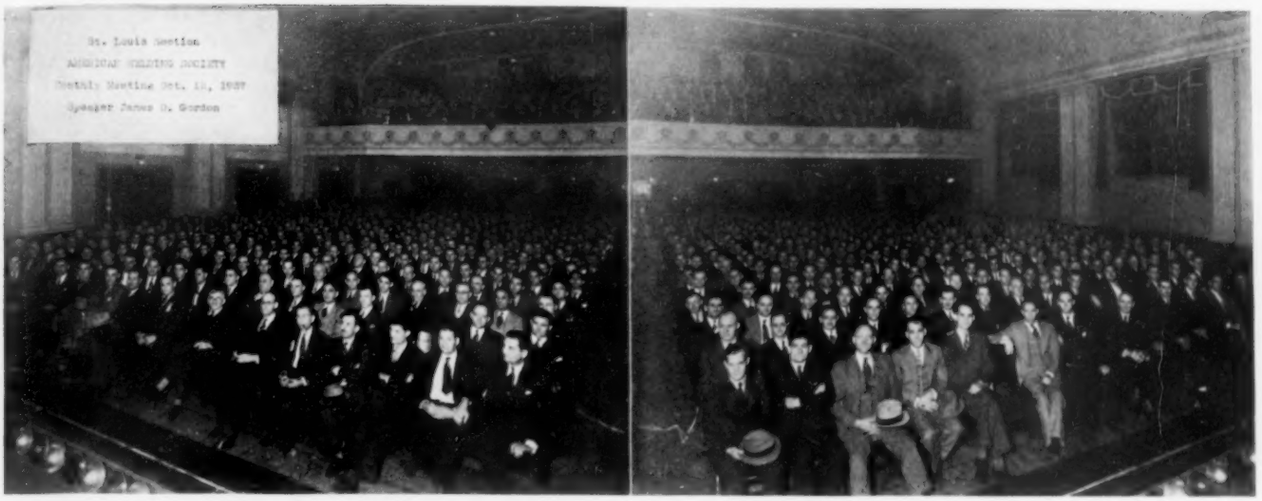
Directors—W. E. Corby, F. C. Fantz, Lockwood Hill, B. H. Leonard, C. W. S. Sammelman and R. C. Thumser.

Section Representative on Board of Directors—A. W. Harris.

The opening meeting of the 1937–38 Season of the St. Louis Section was held at the Moose Hall, 4005 Westminster Place on Friday, September 17th, at 8:00 P.M. "The Advancement of Welding in Industry" was presented by F. C. Fantz, Vice-President of the Midwest Piping & Supply Co.

Using return cards and keeping check on results the Committee was able to handle the extraordinary attendance at the Oct. 12th meeting. There were 1355 present at the meeting which was held at the German House with two other halls being used for refreshments after the meeting. Over 265 firms were represented which showed that the committee had done a fine promotional job. From 8:15 to 8:30 a reel of movies on Spot Welding was shown. Then the speaker of the evening, Mr. J. D. Gordon of the Taylor-Winfield Corp., Detroit, gave his talk on "Re-Design of Simple and Complicated Parts to Fabricated Steel." They broke all former records for their annual educational meeting.

While East at the A. W. S. Convention, Chairman Harris reported that on Nov. 4th the Engineers' Club of St. Louis holds its joint meeting with the Section. F. C. Fantz, Midwest Piping & Supply Co., will speak on "Progress of Welding in Industry." The Section has also been asked to name an Advisory Committee for the Hadley Vocational School to assist in making layout of welding equipment necessary to equip and the selection of Instructors and to guide the course. The Section has worked diligently for months to bring this about and to make use of Smith Hughes Federal funds for this vocational training on welding.



1355 People Attended a Meeting of the St. Louis Section of the American Welding Society

November Meeting held on the 12th at the Engineers' Club. Col. A. S. Douglass Construction Engr., The District Edison Co., delivered an illustrated address on "Welding of Power Plant Piping."

SOUTH TEXAS

The South Texas Section held their organization meeting Thursday evening, October 7th at the Ben Milan Hotel, Houston. The meeting was opened by temporary chairman, Mr. Walter B. Van Wart, who gave a brief introduction outlining the activities of the temporary membership committee appointed at the recent meeting. Regional Vice-President Edward E. Dillman, briefly outlined the aims and benefits of the Society.

A short moving picture was shown by a visitor, Mr. Charles E. Van Wart. This picture showed the complete construction of an all-welded steel storage tank by the Wyatt Metal & Boiler Works, and also showed the fabrication and construction of welded line pipe in the Wyatt Metal & Boiler Works Shop, together with the installation in the field.

The following officers were elected:

Chairman—Marvin Cook, Humble Oil & Refining Co., Houston.

Secretary-Treas.—Malcolm V. Reed, Wyatt Metal & Boiler Works, Houston.

Executive Committee—2 year term:

B. W. Farquhar	G. Steffen
Joe Kane	W. S. Winn
H. M. Stewart	P. J. Kegg

1 year term:

J. J. Netherwood	J. F. Pryor
Frank Fowzer	B. K. Smith
D. R. Pflug	W. G. Baker

WASHINGTON

Chairman—A. G. Bissell, Bureau of Construction and Repair, Navy Department, Washington, D. C.

Vice-Chairman—R. B. Swope, Southern Oxygen Co., Arlington, Va.

Secretary—C. A. Loomis, Bureau of Construction & Repair, Navy Dept., Washington, D. C.

Treasurer—R. F. Wood, Wm. M. Corse, Chem. & Met. Engr., 810—18th St., N.W., Washington, D. C.

Executive Committee—For 2 years:

W. F. Dietz	G. F. Jenks
W. S. Farr	C. M. Underwood
R. P. McMillan	R. K. Wells

For 1 year:

(To be elected after affiliation with Section)	H. W. Hiemke
	G. E. Knox
	H. L. Whittemore (member)

Representative on National Board of Directors—A. G. Bissell.

On Tuesday, September 21, 1937, fourteen Society members and twelve visitors attended a dinner meeting for the purpose of forming a chapter of the AMERICAN WELDING SOCIETY in Washington, D. C. At this time sixteen members of the Maryland Section resided in Washington and its environs. By September 24th the organization committee obtained twelve additional members and made application to the National Organization for authority to operate as a Section of the AMERICAN WELDING SOCIETY. Authorization was granted October 1, 1937.

The following is our program of meetings for the year 1937–38:

October 21st—(Joint meeting with A. I. E. E.) Mr. K. L. Hansen, Harnischfeger Corp., "The Electric Arc as a Welding Tool."

November 2d—(National Officers' Dinner) Mr. Everett Chapman, Lakeweld Inc., "Stresses in Welds and Their Elimination."

December 7th—Mr. Harry W. Pierce, New York Shipbuilding Corp., "Welding Problems in Ship Construction."

January 11th—(Joint meeting with A. I. E. E.) Mr. C. E. Heitman, Budd Mfg. Co. "Thermionic Controls & Resistance Welding."

February 1st—(Dinner Meeting) Mr. C. H. Jennings, Westinghouse Electric & Mfg. Co., "A Comparison of American & European Welding Practices."

March 1st—Mr. G. O. Hoglund, Aluminum Co. of America, "The Welding of Aluminum & Aluminum Alloys."

April 5th—Mr. J. H. Deppeler, Metal & Thermit Corp., "Filler Metals—Electrodes & Thermit."

May 3rd—(Annual Dinner Meeting). Mr. Leon C. Bibber, Carnegie-Illinois Steel Co., "Explosion-Impact Testing of Welded Steels."

The initial educational meeting is scheduled to be held Tuesday, November 23rd, at which time, Mr. A. G. Bissell will outline the various welding processes. Those attending this meeting have been asked to prepare questions on subjects and problems on which information is desired. From the questions handed in the educational committee will outline future discussions and obtain speakers on special phases of welding. It is contemplated to have an educational meeting every month two weeks after the regular meeting.

The total paid-up membership is:

Members	12
Associate Members	41
Operating Members	2
Total	55

YOUNGSTOWN

Chairman—Mr. Charles Watson, President, Youngstown Welding & Engr. Co., Youngstown, Ohio.

Vice-Chairman—Mr. Fred P. McBerty, President & General Manager, Federal Machine & Welding Co., Warren, Ohio.

Secretary—Mr. C. A. Wills, Wm. B. Pollock Co., Youngstown, Ohio.

Treasurer—Mr. John Moriarity, The Linde Air Products Co., Youngstown, Ohio.

Section Director, A. W. S.—Mr. G. A. Reinhardt, Youngstown Sheet & Tube Co., Youngstown, Ohio.

Members of Executive Committee:

Mr. Tross, Youngstown, Ohio
Mr. Emery, Salem, Ohio
Mr. Tam, Greenville, Pa.
Mr. C. Wheeler, New Castle, Pa.
Mr. C. E. McGill, Greenville, Pa.
Mr. Watkins, Columbiana, Ohio
Mr. G. A. Hughes, Youngstown, Ohio
Mr. Warrander, Sharon, Pa.
Mr. Fred Huggins, Warren, Ohio

The Youngstown Section held a meeting on Monday, October 11th at the Ohio Hotel and a very interesting talk was given by Mr. Verg Whitmer, a metallurgist for the Republic Steel Corporation, on Welding Alloy Steels.

At present the Section plans to meet the second Monday of each month and until further notice, meetings will be held in the Ohio Hotel, Youngstown, Ohio.

Sections in Process of Formation

There is shown below, Sections in process of organization and the name of the contact man in each locality. This contact man is either chairman or secretary of a active group interested in the formation and completion of section organization.

CHATTANOOGA, TENN.

CHESTER T. RAYMO, Chattanooga Boiler & Tank Company, 1030 East Main St., Chattanooga

COLORADO

J. H. JOHNSON, Johnson Supply Co., Denver, Col.

COLUMBUS, OHIO

PROF. O. D. RICKLY, Ohio State University

DALLAS-FORT WORTH, TEXAS

E. E. DILLMAN, Wyatt Metal & Boiler Works, Dallas, Texas

FORT WAYNE, INDIANA

JAMES MCCLURE, Wayne Welding Supply Co., 513 East Wayne St., Fort Wayne, Indiana

MIAMI VALLEY (Dayton, Troy, etc.)

E. STANSEL, Frigidaire Division, General Motors Corp., Dayton, Ohio

LOUISVILLE, KY.

A. HURTGEN, Henry Vogt Machine Company, 10th & Ormsby Streets

MEMPHIS, TENN.

B. B. DRURY, JR., Modern Engineering Company, 238 South Front St.

NEW ORLEANS, LA.

O. B. MCLAUGHLIN, Freeport Sulphur Co., Chairman

J. H. BULL, Lester Alexander Co., Secretary

OMAHA, NEBRASKA

L. O. SCHNEIDERWIND, Omaha Welding Company, 1501 Jackson Street

ROCHESTER, N. Y.

THOS. S. GAYLORD, Eastman Kodak Co.

TOLEDO, OHIO

J. W. SHUGARS, The Lincoln Electric Company, 632 Spitzer Building

TULSA, OKLAHOMA

R. R. THOMPSON, Air Reduction Sales Company, Tulsa, Oklahoma

WICHITA, KANSAS

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THE ENGINEERING FOUNDATION
WELDING RESEARCH COMMITTEE

Fatigue Strength of Welded Joints—A Review of the Literature to October 1, 1936

By W. SPRARAGEN* AND G. E. CLAUSSEN†

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SUMMARY

Caution.—This Summary is merely a condensation of the accompanying report, which reviews information now available on the subject of the fatigue strength of welded joints. It is not intended to present conclusions broader than are warranted by the sources of information cited. Further experimentation is needed to amplify, and in some cases modify, the tentative conclusions now submitted. It should be noted that some of the tests were made without recognition of all the variables which may seriously influence results. Unless otherwise stated, the following comments refer to mild steel.

Expressing Endurance Limit

There are two common methods of expressing the endurance limit of welds. (1) The Wöhler method, which plots stress against the log of cycles, the endurance limit being the stress at which the curve becomes horizontal; (2) the cycles method, which defines the endurance limit as the stress that a weld can withstand for an arbitrary number of reversals. For brevity, the following abbreviations are used throughout: Psi = lb. per sq. in.; cpm = cycles per minute. Unless otherwise stated dimensions are given in inches.

Endurance Limits of Welds and Welded Joints

Butt Welds (Arc and Gas).—Endurance limits in rotating bending of 16,000 psi for bare wire, and 30,000 psi for covered wire are common values. These values represent a weld endurance ratio of 0.60 and 0.90 as compared with the endurance limit of base metal (mild steel). Gas welds generally fall between these two limits. Direct stress (tension and compression) fatigue tests give about the same values as rotating bend, but in the test results available there is apt to be less difference between the bare and covered. The endurance ratio of welds in torsion fatigue is about 25% higher than in tension or bending fatigue, but the torsion fatigue limit is somewhat lower. In reversed bending, Roš and Eichinger state that the fatigue limit of welds is 1.4 times greater than in pulsating tension.

Fillet Welds (Arc and Gas).—"Stress raisers" play an important rôle and sometimes completely offset any differences normally expected between the various processes, kinds of filler materials, and, in many cases, between the types of joints. Some general rules for reducing their effect may be offered. Avoid all sharp changes in section, whether in shape of fillets or joints, which would tend to concentrate stresses. As a result of such sharp changes, various types of strap-joint produce very little increase in fatigue strength as compared with a simple butt joint. Transverse fillet welds with covered electrodes (mild steel) have an endurance limit of 16,000 psi as compared with 60% of this figure for bare electrodes. In both cases, the endurance limit of longitudinal fillets is apt to be 15% less than that of transverse fillets. Oxyacetylene welds generally lie

* Secretary, Fundamental Research Committee.
† Research Assistant, Fundamental Research Committee.
This Report is a Contribution of the Fundamental Research Subcommittee.

between the values given for bare wire and covered electrodes, and may approach the values of either, depending upon the type of wire, technique employed, and care with which the welding is done.

Tee Joints.—Taking the fatigue resistance of a solid Tee section as 100%, that of an unchamfered fillet welded Tee is 72%, and of a Tee joint with edges chamfered to facilitate welding, 84%.

Tests of All-Weld-Metal.—The fatigue strength of sound weld metal (except with bare electrodes) is equivalent to rolled steel of the same composition.

Welds at Elevated Temperatures.—Preliminary results indicate that welds in fatigue tests at elevated temperature differ but slightly from unwelded mild steel.

Processes Other Than Gas and Metallic Arc

The fatigue properties of atomic hydrogen welds appear to be the same as for gas and arc welds. Resistance welds, however, seem to develop remarkably high fatigue values, especially in corrosive media. The torsion fatigue limit of flash welds in mild steel equals that of base metal (about 22,800 psi). Pulsating tension fatigue strength of carbon-arc welds in mild steel varies between 14,000 and 21,400 psi, depending upon quality of workmanship. Thermit welds, as indicated by tests of welded rail joints, appear to have reliable fatigue strength equivalent to gas and arc welds.

Correlation of Fatigue with Other Physical Properties

So far, a reasonably close relationship between fatigue strength of welds and other physical properties has not been found. There are indications that good static ductility aids in obtaining good fatigue value by relieving notch effect.

Influence of Defects

Internal Defects.—The adverse effect of internal defects, of which faulty penetration is a special type, is accounted for by their influence in causing local stress concentrations. Internal defects, such as pores and slag inclusions, are almost universally admitted harmful to fatigue properties of welds. Their relative importance is not as yet evaluated, although for well-prepared welds their effect is generally considered primary only when more important factors have been eliminated.

Penetration.—The most important type of internal defect from the standpoint of fatigue of welds appears to be poor penetration, that is, lack of fusion along the scarves and at the root of V and double-V butt welds, as well as of fillet welds.

Interrupted Seams.—From the viewpoint of fatigue, interrupted seams should be avoided. If the factor 0.6 is applied to the permitted fatigue stress in plate metal at the end of a weld, and 0.85 to a continuous seam, it is usually uneconomical to use interrupted fillet welds.

Mechanical Treatment

Peening.—One investigator found that unmachined all-weld-metal deposited by bare electrodes gave 18,000 psi in rotating cantilever tests. This value was raised to 20,000 psi by peening.

Hot Forging.—Hot forging at ordinary forging temperatures is beneficial, but less so at temperatures of about 1200° C. on account of increase in grain size. Hot forging increases the fatigue limit of back-hand gas welds 20%; of fore-hand welds only 10%. Increases of 75 to 100% in fatigue value were found due to forging of welds made with coated and cored electrodes (0.07 C, 0.65 to 2.8 Mn) in mild and low-alloy steels.

Machining.—To date, fatigue tests show that for medium and high quality arc welds, butt or fillet, in structural steel, intelligent removal of undercutting and other surface notches or reinforcement by machining, raises the fatigue value about 25%. In poorer quality welds with high inclusion content, machining appears to be of no advantage One investigator has obtained 40% better pulsating fatigue value from parallel shear fillet welds, the inner ends of which had been machined than from unmachined.

Other Welding Conditions

Scarf Angle.—Scarf angle is important for fatigue value only in so far as penetration is concerned, and it is recommended that scarf angle be as small as possible consistent with good penetration.

V and X.—Single-V welds appear to have better fatigue properties in the as-welded condition. Double-V welds appear superior when stress relieved.

Current and Reverse Run.—Fatigue tests on welds made with different sizes of electrodes show variations that are probably to be ascribed to variations in workmanship. A reverse run (re-welding the root) raises the direct tensile and reversed-bend fatigue strengths in mild and alloy steel by 10 to 20%. The reverse run is important because it eliminates notch-effect at the root of the V, not because it refines the grain structure.

Thermal Treatment

Full Annealing.—Annealing (880 to 920° C.) is detrimental to welds with medium or high nitrogen content, above about 0.04% N_2, but is beneficial when the nitrogen content is below 0.04%. The difference between the as-welded and the annealed specimens was never more than 3000 psi, however.

Stress Annealing.—(Heating to about 600° C. to relieve shrinkage stresses.) The effect of stress annealing may be expected to be small.

Shrinkage Stresses.—In welds with high ductility and yield point, internal stresses are quickly eliminated by plastic yielding under repeated loads. In brittle welds, shrinkage stresses lower the fatigue as well as the impact value.

Carbon Content

Carbon content of the plate has only slight effect on the reversed-bend endurance limit, 21,400 to 22,800 psi, but the endurance ratio: (endurance of weld)/(endurance of plate) decreases from 0.4 with 0.1% C, to 0.2 with 0.7% C.

Alloys

Welds in low-alloy steels have acceptable fatigue value, but they possess little advantage over mild steel in fatigue, except at high values of superimposed tension.

For best fatigue behavior, weld metal and plate should have identical elastic moduli as nearly as possible, in order to minimize shear forces and stress peaks caused by cross-sectional contraction.

Other Alloy Steels.—V butt welds by the atomic hydrogen process in plate containing 0.28–0.35 C, 0.5 Mn, 1.1 Cr, 2.0 Ni, 0.25–0.40 Mo, using the cantilever machine and a rod containing 0.47 C, 1.98 Si, gave a fatigue value in the weld of 25,000–35,000 psi. Fatigue value in general is a function of the composition of filler rod. The comparative fatigue value of chromium-molybdenum electrodes was higher than chromium-nickel, or 3½% Ni electrodes, in plate containing 0.32 C, 3.4 Ni, according to one investigator using the Upton-Lewis reversed-bend machine. Welds in plate containing 3½% Ni withstood 20 times as many cycles at 30,000

psi as plain medium-carbon plate, a low-carbon electrode (0.13–0.18% C) being superior to chromium-vanadium (0.89 Cr, 0.15 V), or nickel-chromium (1.0 Ni, 0.5 Cr) electrodes for both plates.

Austenitic Steels.—The fatigue limit of spot-welded 18–8 is estimated to be 26,000 to 31,000 psi, and of an 18–8 containing 0.1 C, 1.3 Ta to be 37,000 psi.

Cast Steel and Cast Iron.—The rotating-bend fatigue limit of cast steel welds (bare electrodes) is 15,800 psi. The decrease in fatigue strength by welding is proportionately less than the decrease in tensile strength for specimens without cast skin. Annealing is not beneficial to fatigue properties. The cantilever fatigue limit of gas welds, 45° V, in 1-inch cast iron (3.46 total C, 0.74 combined C, 1.33 S, 0.106 Si, 0.66 Mn, 0.282 P) using cast-iron welding rods, was 12,000 psi; the unwelded cast iron gave 13,500.

Brazing.—Reversed bend fatigue limit of a brazed joint in mild steel $^3/_4$ in. × $^3/_8$ in. cross section, was found to be 20,000 psi.

Non-Ferrous Metals

Rotating-Bend Fatigue Limits of Non-Ferrous Acetylene Welds Bartels[38] (1930)

	Tensile Strength (psi)		Endurance Limit (10×10^6 Cycles)		
Material	Unwelded	Welded	Unwelded	Welded	Welded Annealed
Copper	39,000	17,700	12,100	5,700	6400
Aluminum	17,400	13,400	8,500	8,500	..
Silumin	19,500	6,250	7,800	10,700	5000
Copper-Silumin	16,600	10,100	9,300	11,400	..

Corrosion Fatigue

The results show that the rotating bend fatigue limit of welds in mild steel in tap water is usually higher than in air.

Methods of Design

Methods of designing welded structures on the basis of fatigue have been discussed on a number of occasions, especially during the past few years, and have been embodied in the national standards of Germany and Austria, and in important specifications in Switzerland and the U. S. A. The Germans have specified fatigue requirements of filler metal to be used in important specifications, such as railway bridges and boilers. A machined specimen double-V butt weld in mild steel must give a pulsating tension fatigue endurance limit of 24,200 psi, and in low-alloy steel, 25,600 psi, for railway bridges.

The AMERICAN WELDING SOCIETY Bridge Specification permits design stresses in properly made butt joints welded from both sides when subjected to pulsating stresses from zero to maximum of 13,500 psi. When the stresses are alternating, only $^2/_3$ of this value is allowed. There is a 15% penalty in design value in case of single-V backed-up welds. In butt welds subjected to a pulsating shear from zero to maximum, a design value of 9000 psi is allowed which is again reduced to $^2/_3$ if there is a reversal of stress. The same 15% penalty applies to single-V backed-up welds.

Fillet welds subjected to either tension, compression or shear are allowed 7200 psi when the stress varies from zero to maximum, and $^2/_3$ of this figure when the stress is reversed. Only a good grade of heavily covered electrode is permitted.

Repeated Impact

Repeated impact tests were carried out in 1928 by the British Engine, Boiler and Electrical Insurance Company. Welds free from oxides and nitrides gave the best results. Normalizing at 910° C. had little effect.

Creep

Limiting Tensile Creep Stress, psi Stager and Zschokke[190] (1932)

Material	300° C.	400° C.	500° C.
Mild Steel Plate	31,200	15,600	5700
All Weld Metal, Gas	18,500	8,500	1400
All Weld Metal, Arc	19,900	9,900	2800
Welded Joint, Gas	25,600	12,100	5700
Welded Joint, Arc	25,600	15,600	5700

Being a composite of plate and weld metal, the welded joint displays creep properties intermediate between them. Above 400° C., the welded joint is equivalent to mild steel. Summarizing, the creep strength of welds in mild steel is probably little, if any, inferior to unwelded plate up to 500° C., although the initial creep rate may be somewhat higher. Full annealing is not beneficial.

Boilers

Pressure vessel fatigue tests show that fatigue failure inevitably occurs in regions of stress concentrations; e.g., gage plugs, manholes and pads, rather than in the welded seam itself. The only unsatisfactory welds in all the fatigue tests were those made with bare electrodes.

Riveting and Welding

Strengthening by Welding.—The effect of strengthening by welding is not so great in fatigue as in static load conditions. Welding intended to strengthen riveted joints must be designed to take the whole load in order that plastic yielding will not take place in the neighborhood of the weld and lead to fatigue failure. The fatigue strength of welded and riveted joints do not differ greatly. High quality unmachined double-V butt welds have higher reversed-bend fatigue strength than riveted overlapped joints.

Bridges and Machinery

Riveted bridges strengthened by welding are stiffened, the natural frequency being increased 3 to 7% in the loaded and unloaded states. Welding decreases the damping factor, that is, the range of frequencies at resonance, and decreases stresses and deflections due to traffic. The advantages of welding in preventing vibration in machinery are connected with the higher modulus of elasticity of welded steel as compared with cast iron. The closed section, ideal for preventing vibrations, is easy to weld but difficult to cast.

Tubes

The fatigue value of welds in aircraft structural tubing has been investigated by rotating bend tests on individual gas butt welds. Values given vary from 14,000 psi for gas-welded plain carbon and Cr-Mo tubing to 28,500 for plain carbon and 30,000 for Cr-Mo, depending on welding technique and penetration. Filler rods play an important part. Flash welds in Cr-Mo tubing gave 32,000 after stress annealing, but gave low values (13,000) in plain carbon. Lap and fish-mouth joints appear to be at least as good as butt joints, but brazed, soldered and bell-and-socket joints are definitely inferior. Pinned and riveted joints have only 50 to 80% of the fatigue strength of welds.

Fatigue Strength of Welded Joints

Introduction

FATIGUE, or repeated loading, of welded parts is quite common and sometimes dangerous. Developments in welding rods and welding design for many applications are toward increased fatigue strength rather

than increased static strength. This nonpartisan review is intended primarily to show progress as indicated by experimental and service studies of welds under fatigue conditions.

Welds have been tested in almost every type of fatigue machine. Most of the investigators have used the rotating-bend type with four-point (Farmer), or three-point (Foeppl), loading. Neither of these types of tests approximates fatigue conditions generally found in service. This has led many of the European investigators to use machines which apply direct tension and compression to actual structural members. A brief discussion of the various methods of testing and the types of machines used for each type of test is given in Appendix A.

Expressing Endurance Limit

There are two common methods for quoting the endurance limits of welds: 1. The Wöhler method using a plot of stress *vs.* log cycles, the endurance limit being the stress at which the curve becomes horizontal, usually about 2×10^6 cycles for homogeneous welds or 5×10^6 for defective welds in alternating tension and compression, or 10 to 20×10^6 in rotating bend for welds in steel; 2. The cycles method; that is, the fatigue limit is arbitrarily stated to be the stress that a weld withstands for, say, 10^6 cycles. The former is, of course, preferable, but the latter is often used for low-frequency tests. That weld-metal has a clearly defined endurance limit whose was shown by R. R. Moore[1] whose gas weld deposits withstood over 700×10^6 cycles of reversed bending at 23,000 psi without failure. Dutilleul[2] tested two rotating cantilever specimens (V welds in plate having 71,000 psi tensile strength made with an electrode giving a minimum fatigue limit of 18,500 psi) at 15,600 psi. Both withstood 500×10^6 cycles without rupture, although numerous blow-holes were revealed by microscopic examination of the weld.

Endurance Limits of Welds and Welded Joints

Butt Welds (Arc and Gas)

In this country most of the tests have been made on the rotating bend type of machine. Endurance limits of 16,000 psi for bare wire and 30,000 psi for covered wire are common values. These represent an endurance ratio of 0.60 and 0.90 as compared with the endurance limit of base material (mild steel). Gas welds generally fall between these two limits. It should be noted that in Germany endurance values of 34,100 psi have been obtained for gas welds in mild steel representing an endurance ratio of 0.86 as compared with unwelded mild steel plate, and that the bare wire welds are more apt to range from 20,000 to 25,000 psi.

In some alloy steels of good weldable quality these endurance ratios are likely to remain in force but the endurance limits will be somewhat higher.

In reversed bend tests (not rotating) the endurance limits and ratios compared with unwelded steel are about the same as in the rotating bend, providing specimens are carefully machined. Unmachined welds are likely to develop from 10 to 20% lower fatigue strength depending on undercutting, and whether or not reinforcement is gradual. The effect of annealing, and mechanical working is discussed elsewhere.

Direct stress (tension and compression) fatigue tests give about the same values as rotating bend, but in the test results available there is likely to be less difference between the bare wire welds and the coated electrodes. This may be due to additional variables introduced by the investigators, as, for example, the matter of residual stresses, or to the care with which the welds are made. Small imperfections, or lack of penetration at the root of the weld, or undercutting at the surface, will cause a greater variation in the results obtained from two specimens with the same type of electrode than between different types of electrodes, or processes of welding.

It should be definitely noted that not all types of bare wires or heavily coated electrodes, or even gas welding wires, give the same results. It is unfair to average good results obtained with one type of covered electrodes with the bad results of another type. The same is also true of bare wire, or gas welding.

The authors note with a great deal of interest that the average results obtained with bare wire welds in foreign countries are likely to be better than the results obtained with bare wire welds in this country in so far as endurance limit is concerned. The reverse is true for covered electrodes. In the latter case the difference may be due to differences in steels, and techniques as well as to possible superiority in the type of coverings. The difference would be a fruitful field for further research.

The endurance ratio of welds in torsion fatigue is about 25% higher than in tension or bending fatigue, but the torsion fatigue limit is somewhat lower. Tabular results are given in Appendix B. Haigh[3] and Dustin[4] believe that butt welds of ordinary good quality in mild steel plate have about one-half the fatigue strength of the plate itself. In reversed bending, Roš and Eichinger[5] state, the fatigue limit of welds is 1.4 times as great as in pulsating tension.

Fillet Welds (Arc and Gas)

The tests of fillet welds in fatigue have received considerable attention by foreign investigators as have also various types of joints employing fillet welds. It is difficult to draw general conclusions. Many variables are apparently unavoidably introduced which mask the results of the problem under consideration. "Stress raisers" play an important rôle and in many cases offset completely any differences which one may normally expect between the various processes, kinds of filler materials, and, in many cases, between the types of joints. Some general rules may be offered.

Avoid all sharp changes in sections whether in shape of fillets or joints which would tend to concentrate stresses. For this reason various types of strap joints produce very little increase in fatigue strength as compared with the simple butt joint. There is need in this country for tests of large size butt and fillet welds in fatigue. In comparing the static tensile strength of various types of welded joints, investigators agree that these results are no indication whatever of their fatigue resistance. Graf[6] shows that rough cover plates attached by fillet welds actually weaken a butt joint. In butt and fillet welds alike it is important to provide gradual transition of section, smooth surfaces, proper penetration and sound metal free from inclusions and cavities. Isaacs[283] points out that fatigue failures in fillet welds often commence where they cannot be seen, and suggests that working stresses in fatigue may be 100 to 20% of customary static values, depending on number and nature of stress cycles, and type of joint. Transverse fillet welds with covered electrodes have an endurance limit in pulsating tension of 16,000 psi as compared with 60% of this figure for bare electrodes. In both cases longitudinal fillets are likely to be 15% less than the transverse fillets. Oxyacetylene welds generally lie between the values given for bare wire and covered electrodes and may approach the values of either depending upon the type of wire and technique employed.

A great deal of caution must be observed in comparing values obtained from any specimen, large or small, in the laboratory with expected results in the field. For ex-

Pulsator Tests on Butt Welds with Cover Plates—Witt[8] (1935)

Specimen		Welded with:	Probable Pulsating Tension Fatigue Strength, psi
3.6 × 0.40	I	Coated Electrode	20,000–21,400
3.6 × 0.67 2.8 × 0.40 2.8 × 0.48	II	Stabilend (Trade Name) Electrode	18,500–20,000
3.6 × 0.40 2.8 × 0.28	III	Kjellberg OK 37	12,800
3.6 × 0.67 2.8 × 0.40 2.2 × 0.28	IV	Kjellberg OK 37	12,800

ample, in the laboratory even with a large specimen the relative space occupied by the cover straps, or overlapping portions of lap joint is large as compared with the same joint, say, on a ship. This would tend to exaggerate the importance of some of the "stress raisers" encountered in a laboratory as compared with practice, although their importance cannot, and should not, be neglected. It must be remembered that in many cases in practical design the alternative is not a welded joint instead of a solid plate, but a welded joint as against a riveted joint.

A number of general recommendations regarding shape have been proposed. Thus, cover plates tapered to meet the plate are good but plates tapered to a point offer no advantages although the weld area is increased 26%, according to Roš and Eichinger.[7]

Pulsator tests reported by Witt[8] (table above) show that cover plates with parallel shear deposits are not nearly so harmful as plates with normal shear welds, as the table above shows. The fatigue fractures in specimens III and IV occurred at the normal shear fillet welds. In Specimen II, fracture started in the fillet weld or at the inner edge of the butt weld.

The relatively good fatigue qualities of butt welds with parallel-shear cover plates has been shown by Memmler, Bierett and Gehler,[9] but has not been noted by Graf[6] nor by Schick,[10] nor in service.[11] In boilers the unequal expansion between such so-called strengthening plates and parent metal is an additional factor in hastening fatigue failure.

Bierett[12] gives the following summary for insuring good fillet welds:

(1) The ratio of plate to seam cross section should not be greater than 0.4 to 0.5.
(2) For the same seam cross section short thick seams are better than long thin seams.
(3) The more nearly the plate and straps approach the square as compared to the rectangular cross section, the better is the fatigue value.
(4) Channel-iron straps permit thicker seams and hence are better in fatigue. Angle-iron straps are not recommended.
(5) To make seam ends less susceptible to fatigue they should be rounded (see section on machining).

The relieving of fillet welds is not recommended by Graf,[6] but Schick[10] has shown that some types of relieving contribute slightly (10%) to fatigue strength of fillet but not butt welds. Graf[13] showed that as the ratio: $\frac{\text{stress in straps}}{\text{stress in plate}}$ for parallel shear fillet welds decreased from 1.1 to 0.5 the pulsating tension fatigue limit was increased 100%. Below 0.5 there was no further improvement.

T Joints

The best way to improve the pulsating tension fatigue value of T joints is to taper the leg of the T, as shown by Thum[14] and Graf,[6] Fig. 1. A well prepared T joint with tapered leg is equivalent to a butt weld in tensile fatigue and this should be borne in mind when considering the low values reported by Thum and Lipp[15] in reversed bending.

Roberts[16] gives figures for the relative reversed bend fatigue resistance of a solid T section (100%) and unchamfered fillet welded T (72%), and a T joint with edges chamfered to facilitate welding (84%).

Profile or ribbed plate to avoid T joints of web to ten-

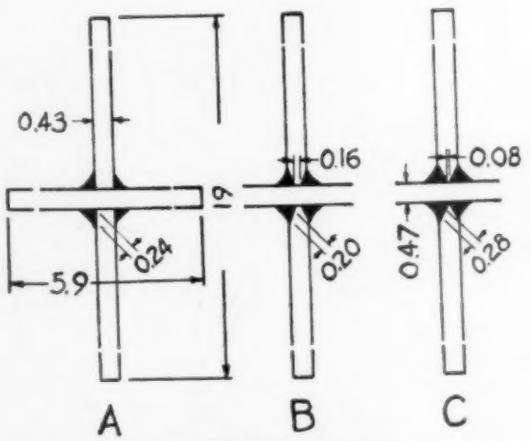


Fig. 1—Arc-Welded Double T Joints

Strength—psi	A	B	C
Tensile Strength	68,500	80,500	83,000
Pulsating Tension Fatigue Limit 2 × 10⁶ Cycles	13,500	15,700	21,400

Low-Alloy Structural Steel Special Electrode (no details) Graf[6]

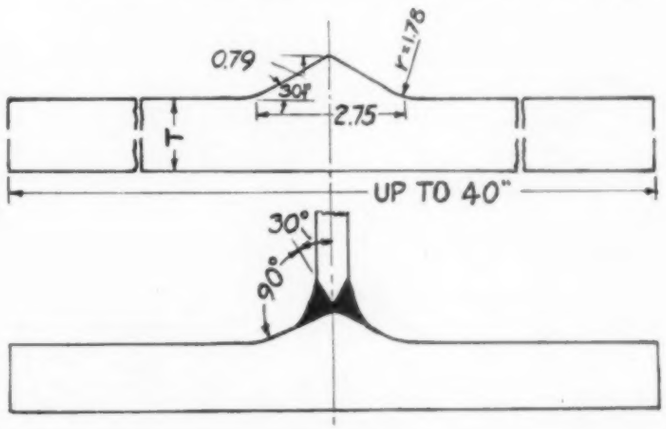


Fig. 2—Ribbed Flange for Welded Parallel Flange Beams; a Type Commonly Produced in Germany. The Tip of the Rib Is Rounded Off. Weight of Rib: 3.7 Lb.-Ft. T Up to 3.55 In. Bürger

sion flange of welded beams is supplied by several German steel works (Fig. 2). Tests by Bühler and Buchholtz[17] having shown that fatigue fracture started in the weld on the upper (less highly-stressed) side of the tension flange, it was believed that simply inserting the web in a profile plate without welding would remedy the trouble. Earlier tests by Schulz and Buchholtz[18] demonstrated that beams constructed of profile plates gave 28% higher fatigue strengths and were superior in bend-fatigue to riveted beams of the same static strength. Bierett[12] showed that stiffeners in welded T beams need not and should not extend into the tension area of the beam. (Consult section on Service Tests for further information.)

Tests of All-Weld-Metal

The fatigue strength of sound weld metal is equivalent to steel of the same composition, as R. R. Moore[1] showed in 1927. In his tests all-weld-metal deposited by oxy-acetylene had about 20% higher endurance ratio than metal deposited by bare electrodes. As stated earlier in this review, one of his all-weld-metal specimens withstood over 700×10^6 cycles at 23,000 psi without failure. The rotating bend fatigue value(up to 50×10^6 cycles) of all-weld-metal deposited by the atomic hydrogen process was reported by Weinman.[19] The highest value was obtained with a filler rod containing 0.46 C, 3.4 Ni whose fatigue limit determined on a machined specimen was between 35,000 and 40,000 psi. Unexplained wide differences between practically identical low-carbon filler rods that were obtained by him may have been caused by differences in degree of soundness. After subjecting an all-weld-metal specimen prepared from ordinary shielded-arc electrodes to 10×10^6 cycles at 30,000 psi rotating-bend, Smith[293] was able to bend the specimen cold through 180° without sign of failure. Hankins and Thorpe[20] found that the rotating bend fatigue limit (25×10^6 cycles) of all-weld metal deposited by a high-grade covered electrode was 18,400 psi whereas an unwelded mild steel of the same static tensile strength (58,000 psi) attained 26,900 psi. The low value for the weld metal is attributed to blowholes and inclusions which have little effect on static strength.

Welds at Elevated Temperatures

The only fatigue tests of welds at elevated temperatures have been made by Lea and Parker,[21] who tested machined welds (70° V) 0.48 in. deep, 0.62 in. wide in a reversed bend, constant bending moment machine at 1000 cpm. at 250 and 450° C., 10×10^6 cycles criterion. The welds were made with reverse run with a covered, shielded arc electrode (analysis not given) in mild steel plate (61,500 psi tensile strength). These investigators also found that understressing raised the apparent fatigue limit of welds.

Temperature ° C.	Fatigue Limit, psi
20	23,300
250	27,300
450	23,500

Lea[22] is also performing fatigue tests on mild steel welds under slowly repeated cycles of stress at boiler temperatures. Preliminary results indicate that welds in such tests do not differ appreciably from unwelded mild steel.

Notched-bar Fatigue Tests

The notch-sensitivity of welds in rotating-bend fatigue has been determined by Matting and Otte.[274] The Lehr short-cycles method was adopted to determine the following fatigue limits (10×10^6 cycles) on cylindrical specimens 0.295 inch diameter. The rolled mild steel had a minimum static tensile strength of 53,000 psi. The peened gas weld was made with a rod containing 0.06 C; 0.24–0.28 Mn; < 0.05 Si; 0.01 P; 0.02–0.04 S; 0.23–0.27 Cu. The double-Veed plates ($8^1/_2 \times 10$ inches) were clamped vertically, the weld begun at the middle and welded simultaneously on both sides by two welders. After $1^1/_4$ to 2 inches were finished the bead was lightly peened. After welding another $1^1/_4$ to 2 inches, the preceding section was reheated to 800° C. by means of torches, and heavily peened with compressed air hammers. After the first half of the weld had been welded in this way the specimen was turned around and the remaining half welded. The air hammers weighed 11 lb. each, and delivered 1200 to 1300 blows per minute at 86 psi pressure. The unpeened back-hand welds were made in the usual way. The normalized specimens were heated 30 to 45 minutes at 920° C. and air cooled. The filler rod for the unpeened gas welds contained 0.11–0.19 C; 0.05–0.09 Si; 0.48–0.93 Mn; 0.01–0.02 P; 0.02–0.05 S. The covered electrodes contained 0.23–0.25 C; < 0.05 Si; 0.64–0.65 Mn; 0.01 P; 0.01 S. They were wrapped with black paper; the coating consisted of compounds of manganese, iron and calcium, with some iron powder, magnesium silicate and water. The notched fatigue specimens had a circular notch, 0.04 inch radius. The results show that notches outweigh all other effects.

Notch Fatigue Tests. Matting and Otte[274] (1936)

Material	Rotating-Bend Fatigue Limit, psi: Unnotched S_U	Rotating-Bend Fatigue Limit, psi: Notched S_N	Notch Sensitivity in Fatigue $\frac{S_U - S_N}{S_U}$
Base Metal (Mild Steel)	30,000	15,600	0.476
Peened Gas Weld	22,800	17,100	0.250
Unpeened Back-Hand Gas Weld	18,500	15,600	0.154
Ditto; Normalized	20,600	17,100	0.208
Coated Electrode Weld	19,900	17,100	0.167

The only other investigators of the notched-bar fatigue strength of weld have been Bartelt[279] (welds in low-alloy and mild steels, with and without notch), and Leitner,[23] who used the M. A. N. reversed bend machine, 10×10^6 cycles criterion. The specimens were 0.59 in. × 0.18 in. × 8 in. long and were machined from welds in mild steel (details of notch and electrodes not given). Test results shown below.

Processes Other Than Gas and Metallic Arc

The published information on fatigue for the remaining welding processes is so scanty that general conclusions cannot be reached. The fatigue properties of atomic hydrogen welds appear to be the same as gas and

Notch Fatigue Tests. Leitner[23] (1936)

Electrode	% O_2	% N_2	Yield Point psi	Tensile Strength psi	Elong. %	Red. Area %	Notch Impact Value mkg./cm.²	Fatigue Limit, psi: Polished	Fatigue Limit, psi: Notched	Decrease
Coated	0.033	0.056	61,000	73,000	27.8	57.5	14.2	39,500	31,000	21.3%
Coated	0.052	0.067	56,000	71,000	19.9	44.9	8.2	28,400	25,800	14.0%
Cored	0.013	0.065	56,000	73,500	19.5	43.8	4.8	31,500	30,600	2.7%

arc welds, according to results given by Weinman,[19] Thornton,[24] Harvey and co-workers,[25] Dorrat[26] and Becker.[27] Resistance welds, however, seem to develop remarkably high fatigue values, especially in corrosive media, as Harvey[25] has shown. Thornton[24] found 25,000 to 27,000 psi for resistance-butt and A.C. flash welds (rotating beam, mild steel) and Vér[28] found 32,800 to 35,600 psi in low-carbon steel (0.05–0.08 C, 0.3–0.4 Mn) also in rotating bend. Rosenberg[29] quotes tests by Behrens who showed that the torsion fatigue limit of flash welds in mild steel was equal to that of base metal (about 22,800 psi) and Kilger[27a] came to the same conclusions concerning reversed-bend fatigue. Baumgärtel and Heinecke[30] also obtained high values of rotating-bend fatigue strength (43,000 to 67,000 psi) in highly-alloyed exhaust-valve steels flash-welded.

Pulsating tension fatigue strength of carbon-arc welds in mild steel, according to Wallmann,[31] varies between 14,000 and 21,400 psi, depending on quality of workmanship. Thermit welds appear to have reliable fatigue strength equivalent to gas and arc welds, as tests of welded rail joints indicate (see section on Rail Joints). The oldest welding process, hand-forging, has not been extensively studied in fatigue, practically the only information being given by Stanton and Pannell[32] in 1911. Their rotating-bend cantilever tests, of comparative value only, showed that hand-forged and resistance-butt (Thomson process) welds were practically equivalent to mild steel and wrought iron; gas welding, at that time a new idea, was not nearly so good. Laboratory fatigue studies of water-gas welds have not yet been reported.

Correlation of Fatigue with Other Physical Properties

The presence or absence of correlation between the various physical properties of structural elements such as welds, often provides an indication of the nature of defects. Up to the present time a reasonably close relation between the fatigue strength of welds and any other physical property has not been found.

The overwhelming majority of investigators, particularly Otte,[27a] report no relation between the fatigue properties of welds and the usual static and impact properties such as yield and tensile strength, ductility in tensile and bend tests, and tensile- and notch-impact value. The National Physical Laboratory, England, for example, state[33] in their Report for 1934 that the static tensile test is of no real value for assessing the fatigue value of welds. There are indications that good static ductility aids in obtaining good fatigue value by relieving notch effect, as Lohmann[34] points out. Graf[6] and Bierett[12] also state that welding rods having high ductility (20% elongation) and a pronounced yield point give good fatigue values especially in welds stressed along their axis, but the relation is by no means close. Wadling[35] believes that a high ratio of yield point to tensile strength is important for good fatigue properties.

Schulz and Buchholtz[18] found that the relation between pulsating tension fatigue strength and static tensile strength was roughly linear for machined welds in a number of structural steels; this, of course, was not true for unmachined welds. Hoffmann[36] stated that there was a close relation between certain static and impact properties and fatigue strength of welds, but his own results did not substantiate his conclusions. X-ray examination, according to Wallmann[31] and Bierett,[12] should not be too greatly depended upon as an indication of fatigue value. As stated in "Impact Tests of Welded Joints," there is no clearly-defined relation between repeated-impact value and other physical properties. There is no relation between fatigue strength and repeated impact value as Roš[37] and Bartels[38] showed.

In view of the failure to detect correlations, little success is to be expected from formulas by means of which fatigue strength can be computed from other physical properties (see section on Methods of Design for purely empirical formulas). As early as 1919, Stromeyer[39] applied his general formula to Abell's results[40] on welds but the attempt was unproductive. Pester and Schulz[41] and others have shown that existing fatigue formulas are of no great value for welds. Erber[42] suggests that his formula for notch fatigue strength may be applied to welds, but has not yet so applied it. The formula indicates that the fatigue value of welds rises with ductility, and that the fatigue strength of welds is fundamentally a notch fatigue strength.

Credit for the investigation of the large effect of undercutting and other stress-concentrating effects at the junction between weld metal and plate must be given largely to German investigators. Among the first clearly to demonstrate the effect were Füchsel[29a] who pointed out in 1928 that undercut in fillet welds was damaging to fatigue value, and Jünger[43] in 1930 who studied V, lap and T welds. A complete investigation has been made by Graf[6] whose micrographs showing fatigue cracks originating from microscopic notches are very convincing. Decreases of as much as 40% in pulsating tension fatigue strength are ascribed to these notches. The removal of the notches, explains the beneficial effect of machining, but careless transverse grinding of a weld may develop, rather than remove, undercut. The German specifications permit undercut to the extent of 5% of plate thickness. Mailänder and Ruttmann,[44] Shepherd and Moritz[45] and Lea,[46] emphasize the general significance of the surface quality of welds on fatigue strength and Driessen[47] observes that fatigue failure of welded structures in pulsator tests invariably starts at the junction between surface of weld and plate. This is also the observation of the majority of investigators, particularly of fillet welds. Hankins[48] notes the effect, but also finds that the roots of fillet welds are sensitive to fatigue failure.

Recommendations for obtaining a gradual transition from surface to plate are given by Graf,[6] and Bierett and Grüning.[49] Gas welding and coated electrodes give more gradual transitions than bare electrodes, and, in fillet welds, an angle of 30° between surface of weld deposit and plate is better than 45°. A smooth, broad, low deposit in butt welds is better than a rough, narrow, high deposit. Figure 3 by Bierett[12] shows the types of

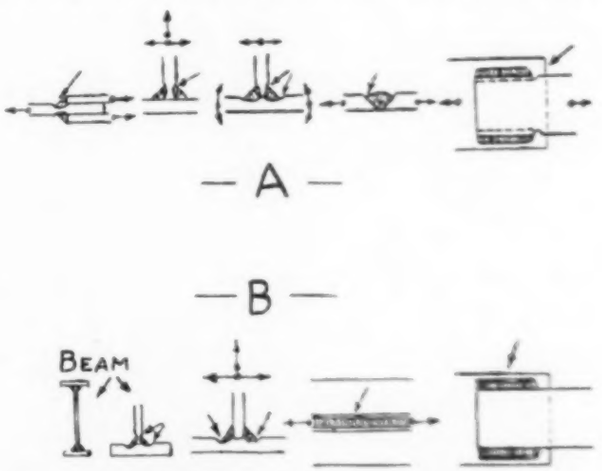


Fig. 3—Lowering of Fatigue Value by Undercutting. Undercut Notches Are Dangerous if They Cut the Lines of Stress at a Large Angle
A—Dangerous Undercut. B—Harmless or Less Dangerous Undercut. Bierett

loading in which undercut notches should be or need not be removed by machining. The bend fatigue tests of Dumas[60] at 10 to 12 cpm. on V-butt welds in mild steel also showed that fatigue cracks usually start at the junction between plate and weld metal.

Influence of Defects

Internal Defects

The adverse effect of internal defects, of which faulty penetration is a special type, is largely accounted for by their influence in causing local stress concentrations. Internal defects, such as pores and slag inclusions, but excluding poor penetration which is the subject of the next section, are almost universally admitted to be harmful to the fatigue properties of welds. Their relative importance is not yet evaluated, although for well-prepared welds their effect is generally considered to be primary only when other more important factors have been eliminated.

The adverse effect of porosity and inclusions in bare-electrode welds is considered by Jennings[51] on the basis of laboratory fatigue tests to be more important than design of specimen. Even with high-class covered electrodes Hankins and Thorpe[20] explain the low fatigue value of welds by the stress raising effect of inclusions and small blow-holes. H. F. Moore[52] and Peterson[53] also note the decidedly disastrous effect of internal defects. The observation that slag inclusions and blow-holes cause service fatigue failures in welds was made as early as 1926 by Schottky[54] and has also been made by Kautz[55] (boiler welds), Pohl and Ehrt[56] (surfacing layers), and Bauer[57] (water-gas welds) among others. Lohmann and Schulz[34] found that fracture followed blow-holes in rotating- and reversed-bend fatigue tests of welds made with bare or coated electrodes, and Matting and Oldenburg[58] made the same observation in pulsating tension fatigue tests. The surface porosity of bare-electrode welds was held to account for their inferiority to welds made by cored or coated electrodes. Using rotating-beam specimens $^5/_8$ in. in diameter with recesses filled with weld metal, H. T. Lewis[59] showed that if the deposit is porous the specimen has a higher fatigue limit when the recess is not filled. Better-class welding (details of welding and recess not given) raised the fatigue strength of the recessed specimen.

But Walmann[31] could detect a difference of only 1500 psi in pulsating tension fatigue strength between machined carbon-arc welds in mild steel (0.12 C, 0.6 Mn) with large and with small blow-holes. Bierett,[12] too, while admitting the importance of pore-free welds, especially for side fillet welds, believes that internal defects simply accentuate the external notch effects, and cites pulsator tests on butt-welded stiffened T beams. In two beams developing good fatigue value the X-ray revealed fine or coarse blow-holes near the tension edge; a beam that gave poor results had defective penetration. Graf[6] gives more emphasis to the notch effects due to absence of reverse welding or to the junction between the surfaces of weld and plate, than to the effects of internal defects. Porosity and notch effect usually occur together. Kruger,[60] as well as Mailänder and Ruttmann,[44] mentions porosity as one of several effects contributing to the low fatigue value of improperly made welds. Normalized, reversed-bend specimens of V welds in mild steel plate (61,500 psi tensile strength), as tested by Lea and Parker,[21] had fatigue values depending on porosity as revealed by micro-, macro- and X-ray-examination. Specimens free from porosity (shielded arc electrode, analysis not given) had a fatigue limit of 28,000 psi, whereas porous welds (electrode analyzing 2.92 Mn, 0.15 C, 0.29 Si, 0.10 Ni) gave only 19,000 psi. Hodge[61] also states that fatigue value is largely dependent on mechanical defects. Welds free from defects as shown by the X-ray had a fatigue limit (details not given) of 30,000 psi; welds with porosity, 16,000 to 18,000 psi.

Two investigators have definitely stated that internal defects are not a factor in the fatigue of welds. Blackwood[62] noted that small gas holes and slag inclusions had little effect on the rotating-bend fatigue value of welds in mild steel made with bare or fluxed electrodes. In pulsator tests on unmachined, unannealed butt welds in $^1/_2$ inch plate (0.1 C, 0.5 Mn) between +21,300 psi upper stress, +2850 psi lower stress, Fry[63] obtained the results shown in the following table.

Pulsating Tension Tests. Fry[63] (1933)

Electrode	Cycles to Fracture	Location of Fracture
Bare	1.2×10^6	Started at Pores in Weld; Spread Through Plate
Dipped	1.0×10^6	Principally in Weld
Heavy-Coated	1.7×10^6	Same as Bare-Electrode Weld

X-ray examination revealed more porosity in the heavy-coated than in the dipped-electrode welds; bare-electrode welds were practically free from blow-holes. Fry concludes that blow-holes and slag inclusions, although they should be avoided, are not important factors in the fatigue value of welds. Poor cast microstructure and high nitrogen content explain the low values of bare and dipped electrodes, Fry believes.

Penetration

The most important type of internal defect from the standpoint of fatigue of welds appears to be poor penetration, by which is meant lack of fusion along the scarves and at the root of V and double-V butt welds as well as of fillet welds. Poor penetration is usually the result of poor or hasty workmanship, as Chapman,[64] Sulzer[65] and Johnson[66] imply, but may also be caused by too narrow a weld angle as in tapered T welds (Bierett[67]), by the use of thick electrodes for the initial layers[28,2] and by other factors.

In Haigh's[68] opinion, poor penetration is the chief factor in lowering fatigue value. Welds with small speck-like inclusions and having an alternating direct tension-compression fatigue limit of ±12,300 to 13,400 psi and a pulsating tension fatigue limit of 21,300 to 22,400 psi are not further affected by the scratching and indenting expected in service. Such a butt weld with a hole drilled through the middle to represent a standard stress-raiser withstood 16.8×10^6 cycles at 12,300 psi and 1.8×10^6 cycles at 17,900 psi before fracture, the cracks following slag inclusions. For joints with poor penetration, however, no fatigue limit can be assigned. Roš and Eichinger[7] also regard poor penetration as more important than small superficial defects such as notches and corrosion pits, which have no further effect on the fatigue limit of welds. Graf[6] found that poor penetration in V and double-V welds is as important as undercutting. The rotating-beam specimens (double-V welds) of Musatti and Reggiori,[69] without exception, broke through the root of the X. The magnitude of the effect of poor penetration is shown by the results of Wallman[31] on carbon-arc welds with shielding gas (referred to in preceding section). Specimens with large blow-holes had a pulsating tension fatigue limit of 19,900 psi; specimens with poor penetration only 14,200 psi.

Poor penetration is also an explanation for many service fatigue failures, as Pfleiderer[70] showed for welded superheater tubes. The relative importance of poor penetration depends on the type of joint and stress,

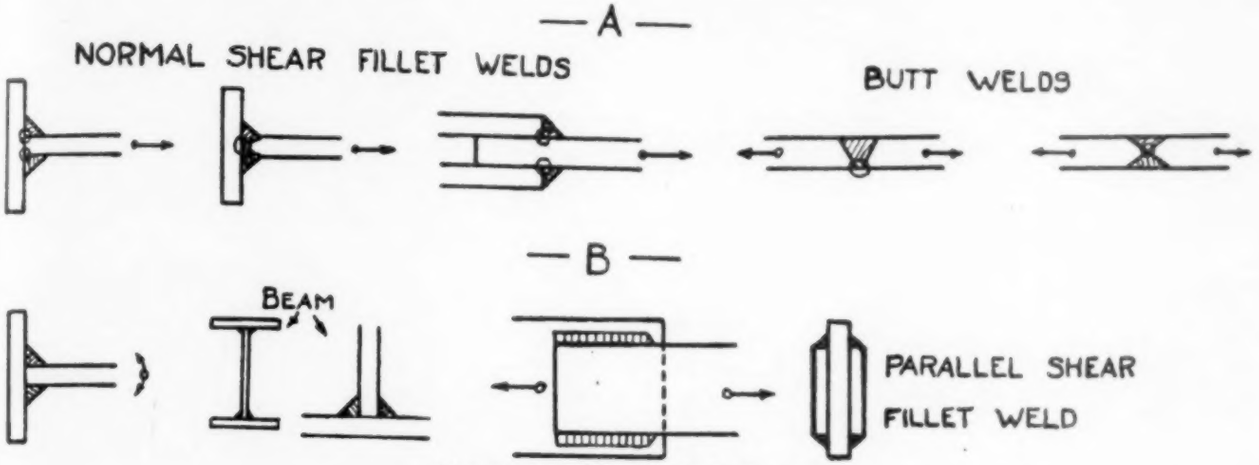


Fig. 4—Lowering of Fatigue Value by Poor Penetration
A—Joints Highly Sensitive to Poor Penetration. B—Joints Insensitive or Less Sensitive to Poor Penetration. Bierett[12]

according to Bierett,[12] Fig. 4. In the lower set of drawings, as in beams, other influences are so much more powerful that the penetration problem is secondary. Covered electrodes aid in obtaining good penetration and consequently good fatigue value, and in keeping slag out of the weld.

It may be concluded that, as Orr's results[71] suggest, poor penetration is not an inherent defect in welds—good workmanship, materials and design may always eliminate this defect—but that, when present, it may decrease the fatigue value up to 50% and more.

Interrupted Seams

The poor fatigue characteristics of interrupted seams were shown by Hochheim[72] in pulsator tests of welded beams. A welded I beam with continuous welds withstood 2×10^6 cycles of bending between +22,200 psi upper stress and +7400 psi lower stress without fracture. A beam of identical construction but with interrupted seams fractured after 60,000 cycles in the same range of stress. The beams were made of low-alloy structural steel (74,000 psi static tensile strength) with special electrodes (type not stated). Graf[305] also showed the poor fatigue value of interrupted fillet welds. Bierett[67] states that interrupted seams should be avoided, and Roš and Eichinger[7] show by an example that, the factor 0.6 being applied to the permitted stress in plate metal at the end of a weld bead and 0.85 applying to the continuous seam, it is not generally economical to use interrupted fillet welds. The adverse effect of interrupted seams on fatigue value appears to be explained by the stress concentrations known to exist at the end of a bead of deposited weld metal. However, Isaacs,[283] on the basis of an investigation of stress distribution but not of fatigue behavior, states that interrupted fillets can be regarded as having a fatigue value very little inferior to continuous fillets, other factors being equal.

Mechanical Treatment

Peening

The effect of peening on fatigue value has been studied by Peterson and Jennings,[73] and Lohmann.[34] The first-named found that unmachined all-weld-metal deposited by bare electrodes gave 18,000 psi in rotating cantilever tests and that this was raised to 20,000 psi by peening. Peening the outer layer was as effective as peening each layer successively, which is in agreement with Bierett,[12,67] and with Strelow's statement[74] that the coarser the grain at the surface of the weld the lower the fatigue strength in reversed bending. About the same increase in reversed-bend fatigue strength as observed by Peterson and Jennings was found by Lohmann in low-nitrogen double-V welds. Gerritsen and Schoenmaker[75] attribute the increase to the closing of pores under the hammer. Peening did not appear to be beneficial in Wilson's fatigue[76] tests of welded girder-to-column connections.

Hot Forging

The effect of hot forging has been closely studied by Becker,[27] Pester and Schulz[47] and Hoffmann.[77] The Lehr short-cycle method was used by Becker to evaluate the rotating bend endurance limits of his specimens; this method has been shown by Bartels[38] to give slightly higher values for welds than the 10×10^6 cycle method. The specimens were oil-cooled during test to maintain their temperature at 20° C. The specimens were machined from 60° double-V welds in $^3/_4$-in. plate (0.1 C, 0.4 Mn, 0.15 Cu) using D.C. arc (bare electrode, 0.08 C, 0.45 Mn, 0.0 Si), gas and atomic hydrogen (filler rod in both cases: 0.10 C, 0.4 Mn, 0.12 Si). The specimens were heated in a gas furnace to three forging temperatures:

800	1050	1200	° C. Furnace Temperature
700	950	1050	° C. Final Temperature

Reductions of 20 and 40% were made using the same number and weight of blows in each case. The results shown in Fig. 5 (the fatigue limit of the unwelded plate was 36,200 psi, given as 100% in the graph) indicate that forging is beneficial but is less so at temperatures of about 1200° C. on account of increase in grain size. Becker explains the effect of forging as an equalization of internal stress and an homogenization of the structure of the weld. The effect of forging appeared to be independent of carbon and manganese content of the filler rods up to 0.32 and 3.15%, respectively.

The effect of hammering double-V gas welds in $^3/_4$-in. mild steel plate (percentage of reduction in forging not stated) has been studied by Pester and Schulz using the rotating-beam machine (10×10^6 cycles). The results are shown in the following table.

Effect of Hot Forging on Gas Welds. Pester and Schulz[41] (1932)

Material	Type of Welding	Endurance Limit, psi	Percentage Decrease
Unwelded	....	24,600	0.0
Unforged	Fore-Hand	18,800	23.7
Forged	Fore-Hand	21,400	13.3
Unforged	Back-Hand	18,500	24.9
Forged	Back-Hand	23,600	5.4

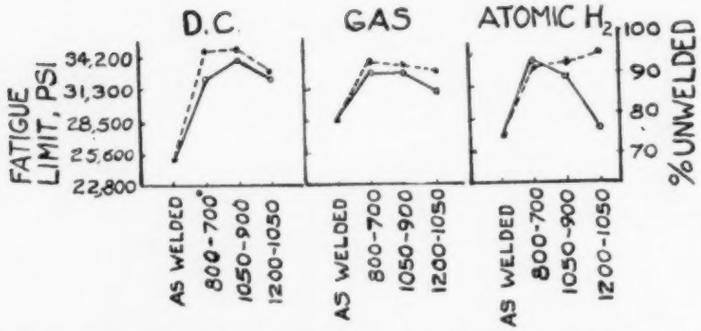


Fig. 5—Effect of Temperature of Forging and Per Cent Reduction on Rotating Bend Fatigue Limit. Becker.[27] Full Line—20% Reduction in Forging. Dotted Line—40% Reduction in Forging

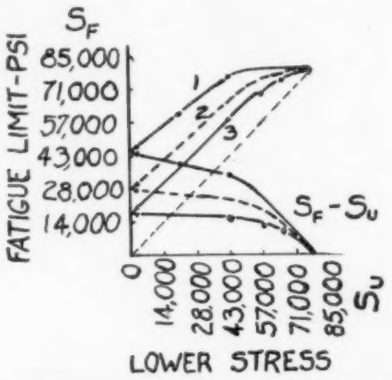


Fig. 6—Pulsating Tension Fatigue Limits (S_f) for Low-Alloy Structural Steel. Curve 1—Unwelded. Curve 2—Transverse Butt Welds (Arc) Machined Flush. Curve 3—Unmachined. Plates 0.4–0.6 In. Thick. Bierett[12]

Hot forging increased the fatigue limit of back-hand welds 20%; of fore-hand welds only 10%. The superior fatigue qualities of back-hand welding in mild steel has also been shown by Kleiner and Bossert.[78] Hoffmann[77] found increases of 75 to 100% in fatigue value due to forging (details not given) of welds made with coated and cored electrodes (0.07 C, 0.65 to 2.8 Mn) in mild and low-alloy steels. Friedmann[79] showed that hammering was beneficial to the fatigue value of welds in Aldrey wire (see section on Non-Ferrous Metals). Bierett[12] states, however, that forging in general has little effect on the fatigue strength of welds (no details given). Johnson[66] and Reiter[80] believe that the good fatigue strength of flash welds is accounted for by the hot work involved in the process. Nevertheless, Etter[81] advises that to avoid cracks, especially in copper but also in mild steel, welding should not be done where shaking of the work is involved, as by riveting at the same time.

Cold Work

The advantage derived by cold working welds has been mentioned on several occasions, for example by Miller,[82] Hoffmann[83] and Hobrock,[84] but this apparently simple method of improvement seems to have received little attention in industry. Hoffmann observed an increase of about 10% in the fatigue strength of shot welds by cold working, and Hobrock believes that it is quite possible that spot welds in Duralumin, cold worked or pre-stressed below the natural elastic limit, may have improved fatigue characteristics. The local application of repeated small loads might not only relieve stress concentrations but might raise the endurance limit of the annealed region adjacent to welds. The former effect has been amply demonstrated by Ludwik, and more recently by Thum and co-workers, and others on a variety of machine elements, such as screw threads, and filleted and drilled shafts.

Machining

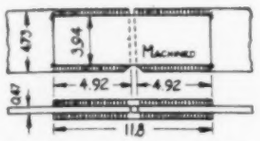
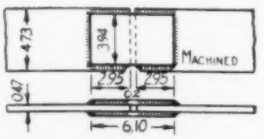
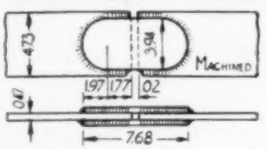
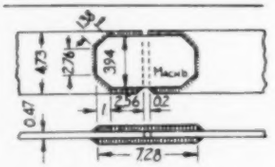
To date, fatigue tests show that for medium- and high-quality arc welds, butt or fillet, in structural steel, intelligent removal of undercutting and other surface notches, or reinforcement by machining raises the fatigue value about 25%. In poor quality welds with high inclusion content, machining appears to be of no advantage.

The effect of machining on fatigue value has been investigated mainly in direct-stress and reversed-bend machines. Results with the former type of machine have been reported by Graf,[6] Schick,[10] Haigh,[68] Hankins and Thorpe,[20] and Memmler, Bierett and Gehler.[9] Graf showed that well-made butt arc welds have a pulsating tension fatigue strength of up to 27,000 psi (2×10^6 cycles) and that this is increased to 34,000 psi by machining the weld flush or by carefully grinding out all notches so that there is a gradual transition from plate to weld cross-section. On the other hand, Schick, using the same type of machine (pulsator) as Graf, found that machined bare-electrode V-butt welds in mild steel had no higher fatigue range (upper stress 31,300 psi, lower stress 20,000 psi, 2×10^6 cycles) at high superimposed loads than unmachined butt welds. This is confirmed by Haigh who tested low-grade welds. Tests by Lea and Parker[21] showed that 70° V welds (slag-coated electrodes: 0.15 C, 0.10 Si, 0.57 Mn) in plate having a static tensile strength of 71,000 psi had a reversed-bend fatigue limit (10×10^6 cycles) of 19,500 psi; when the weld was machined flush with the plate the value rose to 21,500 psi; and when both surfaces of the specimen were machined the value was 24,000 psi. Hankins and Thorpe using high-class covered electrodes and double-V welds found that the pulsating tension fatigue limit unmachined was 17,500 psi; by machining flush the value was raised to 31,200 psi, parent metal giving 34,700 psi. (These values are given as 17,900, 31,200 and 35,800 psi, respectively, in the Report of the National Physical Laboratory for 1933.) The Goodman diagram, Fig. 6, for flush-machined and unmachined double-V welds in two thicknesses of low-alloy, high-strength structural steel emphasizes the increased importance of machining in attaining maximum fatigue values for high-strength structural steel. The graph also shows that the effect of machining becomes negligible with high superimposed static tensile loads. Schmidt[296] states that removal of irregularities from welds by milling may raise fatigue value up to 60% (no details). Machining is also beneficial for austenitic welds, as Kautz[55] has shown (see section on Alloys).

The effect of machining is at least as beneficial for fillet as for butt welds in pulsator tests. The benefit of machining the inner ends of fillet welds is shown in the following table taken from the tests of Memmler, Bierett and Gehler. The welds were made with bare electrodes in mild steel; the radius of the machined ends of the welds was $1^3/_8$ inches. The tests were performed on the pulsating bridge (see Appendix A). As shown in the small diagrams (top of next page) the thickness and breadth of plate metal were $^1/_2$ inch and $4^3/_4$ inches, respectively. Schick also obtained 40% better pulsating fatigue value from parallel shear fillet welds, the ends of which had been machined (details not given) than from unmachined.

An exceedingly informative study of the effect of

Effect of Machining on Fatigue Strength of Fillet Welds. Memmler, Bierett and Gehler[5] (1935)

	Type of Joint	Lower Stress psi S_u	Upper Stress psi S_o	No. of Cycles to Fracture × 10^6	Fracture
	VIA	11,200	22,600	0.31	Inner End of Weld, in Strap
	VIB	11,400	22,800	2.10	Outer End of Strap
	VIIA	11,400	22,800	0.91	Inner End of Weld, in Strap
	VIIB	11,100	22,500	1.63	Normal Shear Weld
	VIIIA	11,900	23,300	1.21	Inner End of Weld, in Strap
	VIIIB	12,100	23,500	2.48	Inner End of Weld, in Strap
	IXA	11,200	23,600	0.64	Inner End of Weld, in Strap
	IXB	11,200	23,600	3.17	Normal Shear Weld

A—Unmachined
B—Machined inner ends

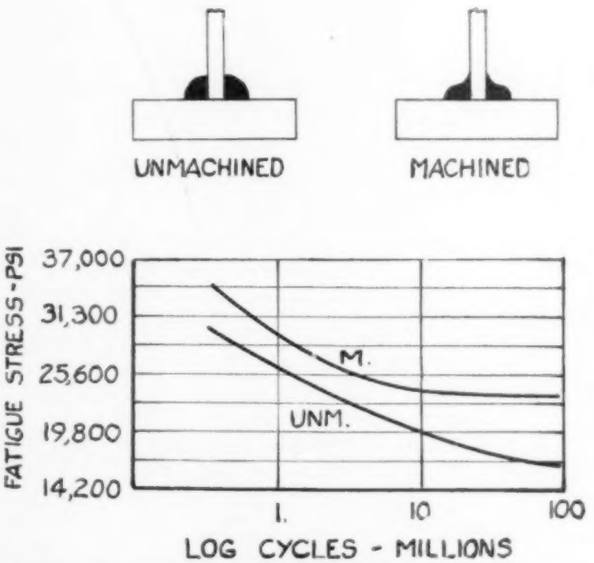


Fig. 7—Wohler Curves for Unmachined (UNM.) and Machined (M.) T's in Reversed Bending. Bare Electrodes. Thum & Lipp[15]

machining on the reversed-bend fatigue strength of T welds has been made by Thum and Lipp.[15] It was shown that Tees (Fig. 7), welded with bare electrodes without tapering of the shank of the T had a fatigue value (10×10^6 cycles) of 15,500 to 17,000 psi, the Wöhler curve not being horizontal. By machining the weld as shown in the diagram the Wöhler curve was horizontal at 10×10^6 at a value of 22,800 to 24,200 psi. Larger Tees (18 in. high, 4 in. wide, $^3/_4$ in. thick) gave a lower increase than the smaller Tee due to removal of the notch by filing or grinding at the transition from weld to plate. The scatter decreased from 20% in the unmachined specimens to 10% in the machined specimens. It was also shown that the reversed-bend fatigue strength of polished round bars of mild steel was not affected by welding with bare electrodes (27,000 psi).

Three other investigators, Roberts,[16] Leitner[23] and Orr,[71] also find that machining raises the reversed-bend fatigue strength of butt welds. Roberts found that by machining bare electrode welds in $^1/_2$-in. mild steel plate to $^3/_8$ in. thick the fatigue value (magnetic impulses synchronized with natural frequency) was equal to parent metal. A small improvement of 10 to 15% due to flush machining was observed in atomic hydrogen welds (Swedish iron rod) as well as in arc welds. According to Leitner, the reversed bend fatigue limit of butt welds (coated and cored electrodes) in mild steel is increased from 23,500 psi unmachined to 30,000–31,000 psi after removal of reinforcement. Orr found that arc welds in high-strength structural steels (compositions not definitely stated) had 65% of the fatigue value of parent metal and that this was raised to 70% by machining. Brown and Orr[85] found that welds machined flush have practically the same reversed bend fatigue limit as unmachined welds, namely, 21,500 psi. Using plate having a rotating bend fatigue limit of 23,600 psi, Townshend[86] found that flush machined X welds had a fatigue limit of 21,400–22,400 psi, but unmachined only 16,900 psi (no material or welding details).

Leitner[87] states that welds prepared with cored electrodes are improved in fatigue value (details not given) up to 4000 psi by grinding off the reinforcement, and Schuster[88] advises that wherever possible the reinforcement of boiler welds should be ground off. Bierett[12] shows that by machining the tension stressed area of a butt weld connecting two mild steel T beams, the fatigue fracture develops in parent metal not near the weld. In general the weld reinforcement should not be removed, the notches and surface irregularities in the weld and its

immediate vicinity being removed by grinding; planing is not usually sufficient because notches are not necessarily removed. The rounding off of the inner ends of parallel shear fillet welds by portable millers is recommended for improvement of fatigue value. Hoffmann[36] also notes the improvement in fatigue value to be expected by machining.

That machining may be undesirable in certain types of welds is shown by Peterson and Jennings[73] who tested low-carbon all-weld-metal deposited by bare electrodes in a rotating-cantilever machine. The unmachined deposit had a fatigue limit of 12,000 to 16,000 psi; machining lowered this to 9000 to 13,000 psi. The decrease was attributed to the exposure of internal pores by machining, surface notches being in general more detrimental than interior notches. It is also possible that blow-holes on the surface may transmit deformations to adjacent material more readily than "constrained" internal discontinuities, especially in impact loading. According to Graf (private communication, Nov. 1936), experience in Germany is that machining is beneficial to fatigue value only in the presence of good penetration and relative freedom from porosity. Porosity exposed by machining is more dangerous than unexposed holes. Therefore, it is German practice to machine only the surface of the weld to remove purely superficial notch effects. The rotating bend fatigue value of gas, arc and flash-welded tubes in rotating-beam fatigue was found to be practically unchanged by machining (see R. R. Moore and Johnson whose work is summarized in the section on *Tubes*).

The reversed-bend fatigue value of low-nitrogen double-V arc welds in structural steel plate was shown by Lohmann[34] to be considerably raised by machining away the reinforcement, as the following table shows:

The Effect of Machining on the Reversed-Bend Fatigue Limits (psi) of Double-V Welds. Lohmann[34] (1933)

Material	Pulsating Bend Fatigue Limit (0 to Max.)		Reversed-Bend Fatigue Limit ± Max.	
	Unmachined	Machined	Unmachined	Machined
0.08 C, 0.5 Mn	25,600	39,800	17,100	24,200
0.18 C, 0.4 Si, 1.0 Mn, 0.7 Cu, 0.5 Cr	32,700	45,500	21,400	24,200

He conceives that it is the purely stress concentrating effect of the reinforcement that accounts for the benefits derived from machining, and gives the following values for artificial reinforcements on plates of the low-alloy steel mentioned in the above table:

Fatigue Tests of Models. Lohmann[34] (1933)

Type of Unwelded Specimen	Reversed-Bend Fatigue Limit, psi
Smooth Surface, Milling Marks Perpendicular to Axis of Stress	±32,700
Artificial Reinforcement 1 Mm. High	±21,300
Artificial Reinforcement 2 Mm. High	±19,900
Artificial Reinforcement 3 Mm. High	±17,100

The figures are revealing but their interpretation must not be too rashly attempted. All results indicate however that in welds, as in other structural elements, removal or addition of metal may have quite unexpected effects on fatigue strength and that the designer and welding engineer should bear them in mind.

Welding Technique

Scarf Angle

Scarf angle is important for fatigue value only in so far as penetration is concerned, according to Bierett[12] who recommends that scarf angle be as small as possible consistent with good penetration. Jennings[89] found the following rotating-bend fatigue limits for machined, low-carbon, bare electrode welds in hot-rolled steel plate having an endurance limit of 27,000 psi:

Rotating Bend Tests with Different Scarf Angles. Jennings[89] (1930)

Type and Angle	Endurance Limit, psi
0°	16,000
30° V	21,000
45° V	20,100
30° X	16,200
45° X	17,800

The specimens were 0.30 inch diameter. Dörnen,[90] using two types of scarves on double-V welds in mild steel plate (Kjellberg electrodes): (*A*) 70° with no spacing; (*B*) 120° with $^3/_{64}$ in. spacing, found that the latter was superior in direct stress fatigue (8 cpm.). At ±20,000 psi unmachined welds of type (*A*) withstood less than 16,000 cycles whereas type (*B*) withstood 50,000 (average of 14 specimens). The scatter was lower for type (*B*) (+100; −40), and only a little greater than for unwelded mild steel. An increase in root spacing as well as the use of small-diameter electrodes for the starting run is also recommended by Schaechterle.[91]

V and X

The relative merit of single-V welds as compared to double V in fatigue is decided by Jennings' tests given above in favor of the single V. Thornton[92] found that double V welds gas and arc in mild steel gave 5000 psi less cantilever fatigue strength than similar single-V welds. Bock[93] also finds that single-V welds are superior to double-V, but only to the extent of 1500 psi for well-made covered electrode welds. Bierett,[67] Roberts,[16] and Roš and Eichinger,[7] however, find that there is no difference between the two types. Roš and Eichinger qualify their statement to include only welds perpendicular to the axis of tension. If the welded seam lies in the direction of tension, differences are revealed as shown in the following table. The stress cycle was between +1500 psi and +27,000 psi; plate thickness; $^5/_8$ inch. The stress anneal was beneficial only for these welds in the direction of stress, not for welds transverse to the axis of tension, which suggests that the high longitudinal shrinkage stresses may have an effect on fatigue behavior. It may also explain the superiority of the double-V joint as compared with the single-V; after both had undergone a stress-relieving treatment.

Pulsator Tests on Unmachined Arc Welds in Mild Steel Parallel to the Axis of Tension. Ros and Eichinger[7] (1935)

Type of Weld	Double-V Butt Weld with V's Offset	V Welds	X Welds	300 Cpm.
As-Welded	716,500	343,500	267,400	Cycles to Failure
Stress Anneal 650° C.	958,800 (Unbroken)	692,800	878,500	Cycles to Failure

The increase in fatigue value to be obtained by using butt welds inclined to the axis of tension is particularly demonstrated by the results of Diepschlag, Matting and Oldenburg,[94] shown in Fig. 8. There is a linear increase in pulsating tension fatigue strength with increase in angle between weld and the normal to the tensile load. This is confirmed by the results of Graf,[6] Figs. 9 and 10 and Bierett[12] for semi-circular and other butt welds. The latter and others believe, however, that the inclined or curved butt weld is justified in locations where sensitivity to production defects is expected; the angle be-

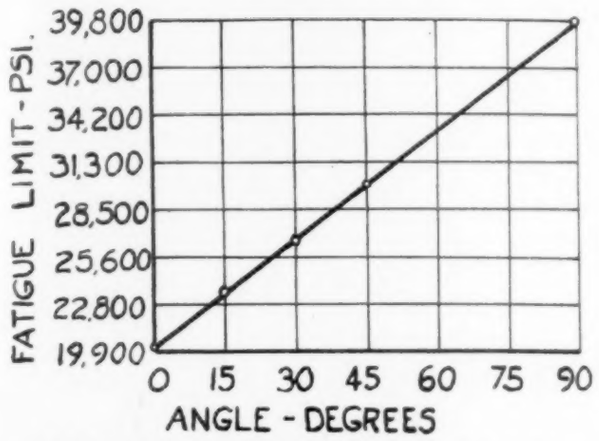


Fig. 8—Relation Between Pulsating Tension Fatigue Limit and Angle Between Machined Butt Weld and the Normal to the Tensile Force. Wazau Hydraulic Pulsator; Coated Electrode; Low-Alloy Structural Steel Cu-Mo. Diepschlag, Matting & Oldenburg[94]

tween weld and the axis of load should never be less than 45°. Furthermore, the magnitude of shrinkage stresses, especially with arc welding, is not known in these joints. Schaechterle[91] and Graf[6] show that failure in static tension in butt welds occurs in the parent metal but in fatigue the butt weld, even the inclined type, starts to fail in the weld.

Current and Reverse Run

Butt welds (30° V) in mild steel made at different amperages, from 200 to 275 amps, with $^5/_{32}$-in. bare electrodes (semi-automatic process) had identical cantilever fatigue limits (±3%) in the tests reported by Jennings.[95] Fatigue tests on welds made with different sizes of electrodes showed variations that are probably to be ascribed to variations in workmanship. Leitner[87] and Vincent[96] show that a reverse run raises the direct tensile and reversed-bend fatigue strengths in mild and alloy steel by 10 to 20%. In the extensive pulsator tests performed by Graf[6] butt welds, arc or gas, that were not carefully reverse-welded had 30 to 50% lower pulsating tension fatigue strength than welds that had been carefully reverse welded. The German investigators in general recommend reverse-welding wherever possible. The reverse run is important because it eliminates notch-effect at the root of the V, not because it refines the grain structure.

Plate Thickness

The effect of increasing plate thickness on the fatigue value of welds is adverse, according to Jennings,[80] who found that a small cantilever specimen 0.469 inch diameter gave 14,400 psi, a large cantilever specimen $2^1/_4$ inches diameter giving only 10,000 psi. Both specimens were 45° double-V welds in mild steel using $^5/_{32}$ in. bare low-carbon steel electrodes, 150 amp. (analysis of plate and electrodes not given). He ascribes this size effect to residual stresses and is concurred therein by Roš and Eichinger.[7] These investigators observed that the pulsating tension fatigue limit of the junction zone material was: Butt welds 19,900 to 21,800 psi; T and normal- and parallel-shear joints 11,300 to 17,100 psi the lower value being obtained for samples from thick plates (about 1 in.), the upper applying to medium size plates (about $^1/_2$ in.). Annealing raises the pulsating tension fatigue value of the junction zone somewhat (no data given). Graf[97] showed that the pulsating fatigue strength of structural steel with flame-cut surface depended on plate thickness, the plate containing a transverse hole. In $1^9/_{16}$-in. plate the value was 27,500 to 29,000 psi; in $3^1/_8$-in. plate, 23,200 to 25,600 psi. H. T. Lewis,[59] however, found that fatigue cracks appeared to about the same extent after 227,000 cycles at 29,700 psi in all three of the equally stressed necks (diameters: 1 in., 0.91 in. and 0.79 in.) in a composite rotating cantilever specimen of mild steel with recesses containing weld metal (no details). The rotating bend fatigue tests performed by Ehrt and Kühnelt[98] on different zones of a shaft (0.66 % C) built up with different kinds of wear resisting electrode deposits confirms Lewis's conclusions.

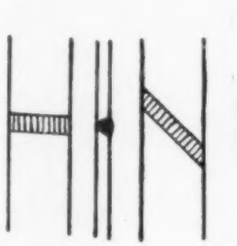


Fig. 9—Pulsating Tension Fatigue Limit, Gas Weld in Mild Steel. Transverse Butt Root Welded—25,600 psi, Not Root Welded—17,100 psi. 45° Butt Root Welded—31,200 psi, Not Root Welded—24,200 psi. $2^1/_4$ in. Wide $^1/_2$ in. Thick. Graf[6]

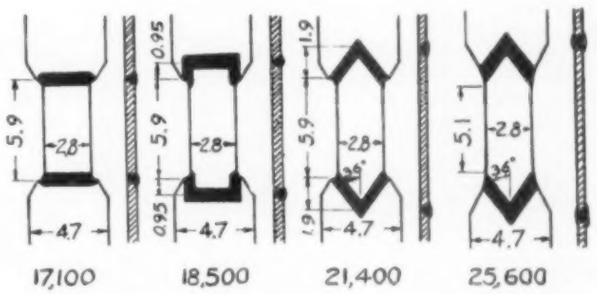


Fig. 10—Pulsating Tension Fatigue Limit (psi) Gas Welds in $^1/_2$-In. Mild Steel. Graf[6]

Fatigue Tests of Weld Elements

To indicate the directions in which the search for means to improve the fatigue properties of welds should proceed, a number of investigations have been made on weld elements and models of welds. The most extensive series of tests on weld elements has been reported by Schulz and Buchholtz,[18] who performed their tests, (summarized in the table on the top of the next page) on a high-strength, chromium-copper structural steel having a fatigue strength in pulsating tension of 39,800 psi as determined on a machined rectangular bar. According to Bierett,[67] other tests have shown that the double-sided run is no more unfavorable than the single-sided and that the transverse is as dangerous as the longitudinal bead. The more longitudinal runs are present the more dangerous they are. He also points out[99] that the loss due to a bead welded on the surface ought not to be quoted as a percentage because the loss depends on plate thickness.

Pulsator tests of three kinds of model "joints" in mild steel using three types of beads have been made by Bühler.[301] As shown in Fig. 11, the throat thickness *A* was the same (0.16 inch) in all models. Model *N* was welded in two layers (100–115 amp. D.C.), Models *A* and *B* in one layer (180 amp. A.C.). All specimens in fatigue failed at the end of the fillet welds so that there was no difference in fatigue value between the three models. Beads of Type 2, however, were somewhat better in fatigue than Type 1, but the former were

Pulsator Tests of Weld Elements. Schulz and Buchholtz[18] (1933)

Element	Percentage of Pulsating Tension Fatigue Strength of Machined Flat Bar
Plate with Hole $^5/_8$-in. Diam.	78%
Plate with Rivet	64
Transverse Bead (Arc) on One side	75
Transverse Bead (Arc) on Both Sides	39
Longitudinal Bead on One Side	43
Longitudinal Bead on Both Sides	49
Stud on One Side	64
Stud on Both Sides	36
Rectangular Strap	34
Rhombic Strap	32

Location of Fatigue Fracture Is Indicated by Wavy Line.

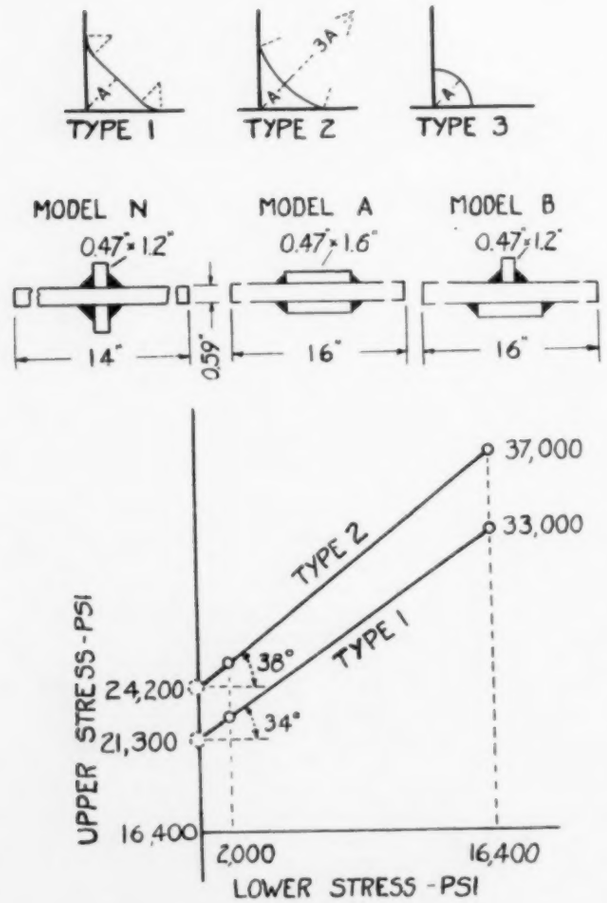


Fig. 11—Results of Pulsating Tension Fatigue Tests on Three Kinds of Models Using Three Types of Beads. Buhler[101]

machined to shape. Type 3 beads were definitely inferior. It must be said that the fatigue values plotted in Fig. 11, especially those for a lower tensile stress of 16,400 psi, are based on relatively few tests. Bühler concluded that the pulsating tension fatigue limit of mild steel in the neighborhood of the usual end and side fillet welds is 21,300 psi, about 0.7–0.8 that of a mild steel bar containing a hole.

The reversed bend fatigue limit of a plate tested by Roberts[16] was reduced from 29,200 psi as received to 15,700 psi after a bead of metal had been deposited across the maximum stress section. After the bead is machined flush the fatigue limit rises to 18,000–20,000 psi. Schaechterle[91] gives the following values for pulsating tension fatigue strength of mild steel and low-alloy structural steel. (Dimensions of specimen: $2^3/_4$ in. × $1^3/_{16}$ in.).

Pulsator Tests of Weld Elements. Schaechterle[91] (1933)

Pulsating Tension Fatigue Strength, psi

Condition	Mild Steel	Low-Alloy Structural Steel
With mill scale	21,300 to 34,200	34,200 to 51,000
With mill scale and central hole	25,600 to 28,500	28,500 to 31,300
With welded bead on one side	22,800	25,600
With welded bead on both sides	17,100	17,100
Double strap normal shear joint. Plate $^1/_2$ in. thick. Straps $^3/_8$ in. thick.	11,400	11,400
Ditto with tapered seams and machining	17,100	17,100
Butt weld; V or double V	22,800 to 25,600	22,800 to 25,600

In pulsating bending, the reduction due to beads of weld metal is very large according to Hochheim[100] whose results are given below:

Pulsating Bend Tests of Weld Elements. Hochheim[100] (1935)

Lower Stress	Upper Stress, psi	Pulsations
17,100	51,500	2×10^6 Unbroken
8,600	25,600	2×10^6 Unbroken
8,600	25,600	730,000 Fracture

In specimen 2, two beads of metal were run across the bottom of the bar; its endurance limit was close to 25,600 psi upper stress, 8600 psi lower stress.

These tests are exceedingly interesting from a practical standpoint but may be explained by stress raising due to shape, notch effect due to undercutting, shrinkage stresses (none of the specimens appears to have been annealed), or to microstructural changes depending on the inclinations of the interpreter. Less objectionable are the fatigue tests of models of butt welds in reversed bending by Lohmann (Page 12) and by Schulz and Buchholtz. These tests indicated that, for the thickness of plate used (not stated), an artificial "reinforcement" $^1/_{12}$ inch high machined in unwelded plate in medium-carbon or low-alloy structural steel reduced the fatigue value by 30%. A perfect unmachined, reinforced weld, therefore, will probably have a much lower reversed bend fatigue strength than base metal. Baud[101] states that the points of maximum stress concentration indicated photoelastically in models of welds are confirmed by fatigue tests, but the degree of magnitude of concentration is much less in the fatigue test than in the photoelastic specimen.

Thermal Treatment

There are three types of heat treatment applied to welds: 1. full annealing, 2. stress annealing, 3. hardening and tempering. By full annealing above Ac_3 the static strength of the weld is generally decreased, internal stresses are removed, and grain size is refined. Stress annealing has much less effect on grain size and static strength than full annealing. Quenching and tempering is generally applied only to welds in medium- or high-carbon alloy steels. The effect of the first two treatments on the fatigue strength of welds is a matter of controversy, but the beneficial effect of the third type of treatment on welds in aircraft tubing and rail steels is shown by Ward,[102] Beissner[103] and Reiter.[80] Brenner[104] also states that welds in aircraft steels should be heat-treated for best fatigue behavior.

Full Annealing

The effect of full annealing on the rotating- and reversed-bend strength of arc welds in mild and low-alloy steels has been studied by Lohmann.[34] His results show that annealing (880 to 920° C.) is detrimental to welds with medium or high-nitrogen content, above about 0.04% N_2, but is beneficial when the nitrogen content is below 0.04%. The difference between the as-welded and the annealed specimens was never more than 3000 psi, however. Lohmann and Schulz,[34] and Hodge[61] (the latter gives no details), however, have found that there is no connection between the nitrogen content of welds and their fatigue value. The results of French suggest that the fatigue value of age-susceptible welds may be raised by tensile over-stressing. In materials that are not susceptible to stress-aging, tensile overstressing lowers the fatigue value. Bartels[105] showed that the rotating-bend value of gas welds in mild steel and cast iron was not improved by annealing, that welds in silumin were adversely affected, and that welds in copper were slightly improved by full annealing. Brown[106] also showed that full annealing was disastrous to the reversed-bend fatigue limit of gas welds in $^5/_{16}$ inch mild steel plate, the fatigue limit as-welded being ±25,000 psi, and after annealing only ±14,000 psi. Annealing lowered the reversed-bend fatigue limit in double-V arc welds in mild steel from 19,200 to 15,500 psi, according to Lehr.[107] But Sulzer,[65] using the rotating-beam machine found that arc-welded mild steel was raised from 14,500 psi as-welded to 22,400 psi after full annealing. Matting and Otte[274] also found that normalizing at 920° C. raised pulsating-tension and rotating-bend fatigue limits of gas welds 2000 to 3000 psi. Furthermore, Burn,[108] and Schulz and Buchholtz[18] recommend the full annealing of welded structures to avoid service fatigue failures.

The effect of annealing is closely related to the grain size, as Peterson and Jennings[73] have shown. By annealing bare-electrode all-weld-metal for 2 hours at 1700° F. they observed a 30% decrease in cantilever-fatigue limit which they ascribed to coarse grain, although Harvey and Whitney[25] could detect no effect of grain size on the corrosion fatigue of mild steel. This effect of coarse grain on fatigue limit was developed by Thornton[24] as a theory of the fatigue failure of welds whereby the difference in grain size and hardness between weld and base metal is said to account for the low fatigue strength of welds. The grain size effect has however been shown by Lohmann and Schulz[34] and others to be secondary, for weld fatigue failure in rotating- and reversed-bend tests occurs usually through the middle of the weld; sometimes the first crack originates at a point of high stress concentration.

The tensile- and non-reversed or pulsating bend-fatigue results of Graf[97] on flame-cut surfaces in structural steel (65,000 psi static tensile strength) show that the increase in grain size due to flame cutting is not an important factor. The plates with oxy-cut surface have a fatigue value equivalent to a plate with a rivet hole, namely, 27,500 psi in pulsating tension, and 28,500 psi in pulsating bending. After heating for one hour at 885° C. followed by air-cooling, the section being $1^1/_2$ in. × $1^1/_8$ in., the grain size at the surface was refined and the pulsating bending fatigue limit (0 to max.) rose to 37,000 psi. However, by grinding the rough oxy-cut surface to remove surface irregularities without removing the zone of coarse-grained material, the pulsating-bend value was raised to over 55,000 psi. Graf, therefore, concludes that oxy-cut surface need be machined only deep enough to eliminate surface irregularities; there is no need to remove the coarse-grained zone. Similar tests[97A] on flame cut surfaces of low-alloy structural steel (static tensile strength 74,000 psi) showed a fatigue limit of 34,100 psi compared with 38,400 psi for a sawn surface. Ground or milled flame cut surfaces had intermediate values. Melhardt[109] found that oxy-cut surfaces in mild steel had slightly better reversed bend fatigue strength than planed surfaces, tested parallel or perpendicular to the direction of planing. At a reversed stress in the extreme fibers of ±31,300 psi the oxy-cut surface withstood 2.25×10^6 cycles to fracture whereas the parallel and transverse planed specimens failed after 2.0×10^6 and less than 1×10^6 cycles, respectively.

The annealing effect of gas welding on the adjacent plate was found by Kleiner and Bossert[78] to reduce the rotating bend fatigue limit up to 10% in the zone heated above 400 to 500° C., which coincides approximately with the recrystallization of the mild steel plate that was used. This decrease extends over a 70% wider zone in fore-hand than in back-hand welds. These investigators believe that the coarse grain structure of gas welds prevents their attaining the same fatigue value as the base metal. Of the contrary opinion are Musatti and Reggiori,[69] who supply experimental proof in the following table.

Rotating Bend Tests. Musatti[1] and Reggiori[69] (1934)

Specimen	Brinell Hardness	Fatigue Limit psi	Tensile Strength	Endurance Ratio = Fatigue Limit / Tensile Strength
Parent Metal	170	41,000	85,500	0.48
Double-V Weld	...	27,600	81,500	0.34
All-Weld-Metal	...	29,000	83,700	0.35
Overheated Parent Metal	220	47,000	100,000	0.47

Base metal contained 0.23 C, 0.8 Mn, 0.3 Si; the electrode contained 0.08 C, 0.4 Mn, 0.01 Si and was coated with a mixture containing 12% $CaCO_3$, 19% Fe-Mn (80% Mn), 3% Pyrolusite, 37% red Hematite, 6% Fe-Si-Ti, 23% Sodium Silicate (90% accounted for). The overheated base metal had a coarse Widmannstätten structure which, however, did not injure fatigue strength. Other considerations point as well to the relative unimportance of the metallographic structure of welds in mild steel so far as fatigue is concerned.

Occasionally the fatigue failure of welds has been observed in the heat-affected transition zone between weld and base metal, especially in the overheated structure of tubes (Franke,[110] Baumgärtel,[111] Ward,[102] Hoffmann[112]). The heat-affected zone, it is supposed may act in two ways.

(1) The Widmannstätten structure in the over heated zone is undesirable in fatigue. Existing fatigue

values of unannealed cast steel or overheated rolled steel show that in neither material is the fatigue limit below that expected from the static tensile strength. Indeed, Vér[28] demonstrated that slip lines, which precede fatigue cracks, form less readily in the Widmannstätten structure of flash welds in low-carbon steels during rotating bend fatigue tests than in the as-rolled structure. Rosenhain[113] has suggested that the weakest zone in fatigue is that in which, due to the welding heat, cementite has just begun to spheroidize. Observations of fatigue fractures can scarcely be said to confirm this hypothesis. Fatigue fracture in weld metal at room temperature is transcrystalline.[274]

(2) The difference in grain size, structure and physical properties between base metal and weld acts in some way as a source of stress concentration which lowers the fatigue value. This hypothesis is stated most convincingly by Diepschlag, Matting and Oldenburg[94] and mention also by Thornton,[24] who noted that fatigue cracks never started in large Widmannstätten grain boundaries, and by Brenner[104] and Schaechterle.[114]

Kleiner and Bossert,[78] and Roš and Eichinger[7] showed that the heat-affected zone had a fatigue value intermediate between weld metal and plate, the former showing that in gas welds in mild steel the rotating-bend fatigue value of the weld was 30 to 50% below plate metal, the transition zone only 10% below plate metal. Bierett and Grüning[49] also point out that if the heat-affected zone were the cause of fatigue failure, the procedure of depositing concave weld beads with smooth gradual surface tensions which produces a larger heat-affected zone should be expected to lower the fatigue value. Actually the fatigue value is considerably increased.

The two types of explanations may perhaps suffice for the very small proportion of fatigue failures that demonstrably occur in the heat-affected zone of welds. It cannot be said that the presence of this zone is more than a minor factor in the fatigue of welds. In this connection, Vér[28] found that in flash welds in medium-carbon steels, fatigue cracks had a strong tendency to develop and spread in previously-formed slip lines in the network of ferrite. The slip lines tended to occur in the direction of maximum shear stress. The wider the ferrite network, the lower the fatigue strength because slip lines form more readily. Jasper[187] also found that the rotating bend fatigue limit of specimens cut from transition zone and weld metal in arc welds was slightly higher than that of base metal.

Stress Annealing

Full annealing having only a slight beneficial effect on fatigue strength in low-carbon steels, and then only in low-nitrogen welds, the effect of stress annealing may be expected to be small. This is confirmed by all who have studied the problem. Peterson and Jennings[73] found that the cantilever fatigue limit of unmachined bare electrode weld metal was scarcely affected by stress annealing for two hours at 1000° F. Thornton,[92] Roš and Eichinger,[5] Orr[71] and Aysslinger[115] found that stress annealing was beneficial. The first-named found that a covered electrode low-carbon V weld which had a cantilever fatigue limit of 27,000 psi was raised to 28,500 psi by stress annealing. The results obtained by Orr (tabulated in the section on low-alloy steels) indicate an increase of up to 15% due to stress annealing 1/2 hour at 600° C. in reversed bend fatigue limit of welds in low-alloy steel. The pulsating tension fatigue limits of gas and arc welds, V or U (reverse welded) or X joints in mild steel, according to Roš and Eichinger, are:

Pulsator Tests. Ros and Eichinger[5] (1936)

Direction of Stress	Unannealed	Stress Annealed 650° C.
Butt weld perpendicular to axis of tension	18,500 psi	21,400 psi
Butt weld parallel to axis of tension	22,800	25,700

Coated electrode butt welds (10 × 70 mm. cross section in structural steel plate having a pulsating tension fatigue strength (2×10^6 cycles) of 29,900 psi) were raised from 21,400 psi as-welded to 22,800 psi after stress annealing at 500–600° C., according to Aysslinger.

Barnes[96] and Lohmann and Schulz,[34] on the other hand, found that stress annealing is definitely harmful. Using 1/4 in. 3 1/2% nickel and medium-carbon steels, Barnes found that stress annealing at 600° C. lowered the number of cycles withstood by dipped electrode welds on the Upton-Lewis machine at 30,000 psi by as much as 100% in several cases; bare electrode (1.0 Ni, 0.5 Cr) welds suffered least by stress annealing. The reversed- and pulsating-bend fatigue strengths of machined low-nitrogen arc welds in mild steel plate (0.08 C, 0.5 Mn) were lowered from 24,200 and 39,900, respectively, as welded to 21,400 and 38,400 psi after stress annealing at 650° C., according to Lohmann and Schulz.

The results show that the genuine beneficial effect of stress annealing on impact behavior and bend values does not extend to fatigue characteristics. Opinions based on experience and theoretical considerations are more numerous than test results and are summarized in the next section on Shrinkage Stresses. Whether and to what extent the pulsating tension fatigue strength of the weld can be raised by annealing depends on process and materials, according to Graf.[6]

Shrinkage Stresses

Shrinkage or residual stresses are actual stresses, usually local in character, existing in plate and weld metal during or after welding. In the welding of unrestrained parts, the stresses are confined to the vicinity of the welded seam itself; the shrinkage stresses due to closing welds in a rigid structure may be distributed throughout the structure. Shrinkage stresses are created by local heating into the "plastic" state followed by cooling, and must be clearly distinguished from shrinkage cracks and from plastic permanent deformation due to shrinkage at elevated temperatures. At the present time the extent and even the character of the effect of shrinkage stresses on fatigue limit appear to be matters of debate. Some of the available information is summarized in other sections (see sections on stress annealing and on tubes).

Of those who have discussed the subject, the majority believe that shrinkage stresses have a significant effect on the fatigue value of welds. However, Johnson[66] and Roš and Eichinger[5,7] adduce almost the only experimental evidence. Johnson found that the rotating bend fatigue limit of flash-welded Cr-Mo aircraft tubing having an as-welded value of 24,000 psi was raised to 32,000 psi by stress annealing at 950° F. Roš and Eichinger's results have been discussed in the section on V and X welds. The vibration tests of Messrs. Accles and Pollock, Birmingham, England, as described by Roosenschoon,[116] showed that the high shrinkage stresses in welded structures of chromium-molybdenum aircraft tubing were responsible for poor vibration resistance. Boetcher,[117] Kinkead,[118] Jacobus,[119] and Stone and Ritter[120] believe, on the basis of service results (details not given), and Becker[27] and Schaechterle,[121] on other grounds, that shrinkage stresses detract from fatigue value. The failure of a drive shaft (unalloyed steel,

207 Brinell) was ascribed by von Schwarz[281] to internal stresses induced by a collar fillet welded to the shaft. Admittedly, the weld in this instance was improperly made and shrinkage stresses could have been, at most, only a contributing factor.

Those of opposite opinion have presented more evidence, but the problem is by no means solved. Peterson and Jennings[73] proved that shrinkage stresses were not the cause of the low rotating-bend fatigue value of unmachined all-weld-metal deposited by low-carbon, bare electrodes. Lohmann and Schulz[34] also regard shrinkage stresses as of no importance in fatigue because all fatigue failures in rotating- and reversed-bend followed blow-holes. Stress annealing had a negligible effect on fatigue value. On the basis of pulsator tests of welded, stiffened I beams, which were superior in fatigue value to riveted beams, Schulz and Buchholtz[18] concluded that shrinkage stresses cannot be very important. This may be partly explained by the results of Dörnen[122] that the shrinkage stress in welded beams may be less than in rolled sections of the same dimensions.

Schulz and Buchholtz have also found that internal stresses in unwelded, quenched bars are somewhat equalized during fatigue testing. Buchholtz[123] as well as Graf[6] states that in welds with high ductility and yield point, internal stresses are quickly eliminated by plastic yielding under repeated loads. In brittle welds shrinkage stresses lower the fatigue as well as the impact value (no details given). Buchholz states that stress concentrations due to notches, unlike shrinkage stresses, are not removed by cyclic loading. Orr[71] and Boulton,[124] however, are of the opinion that stress concentration factors obtained by photoelastic and similar studies do not apply to actual welds under conditions of fatigue because as soon as plastic yielding occurs the unfavorable stress distribution is relieved. Possibly this is connected with the effect of understressing in raising the apparent fatigue limit, noted by Vér[28] and Bartels[38] for welds. Bernhard,[125] in his portable pulsator tests of riveted bridges strengthened by welding, observed no effects that he could attribute to shrinkage stresses.

A good demonstration of the release of stresses during fatigue tests was given by Siebel and Pfender,[126] who measured the permanent bulging outwards of a gas-welded patch on a boiler drum during a pulsating pressure test. The patch caused a considerable bulge inward of the shell and the shrinkage stresses were estimated (not measured) to be beyond the yield point. Measurements of the bulge were made at various times during the pulsating-pressure test, the stress pulsating between 2140 and 19,900 psi. The first cycle of stress caused a recovery of nearly 1% although the stress was well below the yield point of the boiler plate (35,600–37,000 psi). Recovery during additional cycles was comparatively small and after 10,000 cycles the patch still bulged in.

Siebel and Pfender also developed a known internal stress in a flat bar (8 in. wide, $^9/_{16}$ in. thick) by heating both sides with welding torches. After loading statically to 28,400 psi the internal stress in the direction of breadth was decreased from 35,600 psi maximum to 22,700 psi maximum. After 10,000 cycles at a lower stress of 5700, upper stress of 28,400 psi, further release of internal stress occurred, particularly in the axis of tension.

Bierett[12,67] deduced from the fact that, for the same steel ($S_f - S_s$) in Goodman diagrams (see section on machining) decreased more rapidly for welds lying in the axis of tension than for welds transverse thereto, that the shrinkage stresses, which are close to the yield point, parallel to the seam have more effect on fatigue than shrinkage stresses perpendicular to the seam. Bierett concludes however that if shrinkage stresses are not too high they have little effect on fatigue. He also shows that the shrinkage stresses in plates free to move during welding, Fig. 12, put the edges in compression. This is a protection to the edge of the seam where fatigue failure usually starts. The usual laboratory specimen cut from a large plate does not have this protection. Rosenthal[300] believes that high shrinkage stresses are inelastically relieved and that low shrinkage stresses have no effect on the fatigue value of welds.

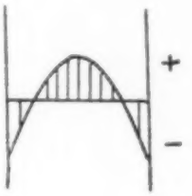


Fig. 12—Shrinkage Stresses in Butt Weld. Bierett[67]

Kautz[55] determined that stress annealing, which is intended to raise toughness and impact value, is not required for austenitic welds in non-aging boiler plate; shrinkage stresses have no effect on fatigue because they are largely eliminated by the first loading and besides are partly relieved at ordinary operating temperatures. Kautz bent an unwelded mild steel bar in an arc, annealed the bar, and then bent it straight again. The internal stress distribution thus created resembled welding stresses. This stressed bar had the same fatigue limit in pulsating tension as a normalized, unstressed bar. This was in agreement with pulsating pressure tests of as-welded boilers in which the fatigue limit was close to that obtained on pulsator specimens. Kommerell[143] came to similar conclusions on the basis of similar tests.

Although the experimental evidence appears to support the view that shrinkage stresses play a minor part, if any, in the fatigue characteristics of welds, the problem is by no means solved. Service experience of welded structures under conditions of nearly pure fatigue loading has shown the necessity for stress annealing in certain cases. When fatigue test results on a wider variety of welds and welded structures under different ranges of stress with respect to yield strength become available, it will be possible to reach more definite conclusions than at present.

Carbon Content

The effect of carbon content on the fatigue value of welds in steel has not yet been systematically studied. Zeyen[127] tested unmachined welds in plate containing 0.1 to 0.7% C in reversed bending; he used the same heavy-coated electrodes (0.1 C, 0.4–0.6% Mn) for all steels. His results indicated that the carbon content of the plate had no effect on the endurance limit 21,400 to 22,800 psi, but that the endurance ratio: S_{fw}/S_{tww} decreased from 0.4 with 0.1 C to 0.2 with 0.7 C. His results are given in the table on the top of the next page.

Vér[28] also found that the fatigue limits of polished flash welds in rotating bending (Retjö-Csonka oil-pressure machine) was 28,400 to 33,400 psi in the range 0.05 to 0.64% C, the 0.64% C specimens giving 29,900 psi. The ratio of fatigue to tensile strength of the welds decreased from 0.51 (0.08% C) to 0.29 (0.64% C). Specimens containing 0.86% C had a higher fatigue limit, 39,000 psi, but a lower ratio, 0.28.

A steel containing 0.08% C (tensile strength 58,000 psi) had a slightly higher rotating beam fatigue limit with cored or bare electrodes, according to Lohmann and Schulz,[34] than a plain-carbon steel containing

Reversed Bend Fatigue Limit (10×10^6 Cycles) of Butt-Welded Carbon Steel Plate. 0.24 In. Thick. All Specimens with Mill Scale and Unmachined. Zeyen[127] (1936)

	Unwelded		Arc-Welded				Gas-Welded
Carbon Content of Plate %	Tensile Strength psi	Reversed Bend Fatigue Limit psi	Heavy Covered Electrode (1)	Lightly Covered Alloy Electrode (1.5% Mn) (2)	Cored Electrode Alloyed (2)	Austenitic Electrode Nichrotherm (3)	Alloyed Filler Rod (4)
0.11	57,000	22,800	22,800	22,800	19,900	18,500	22,800
0.30	81,000	28,500	21,300	Not determined	19,900	Not determined	28,500
0.38	92,500	29,900	21,300	21,300	21,300	19,900	25,700
0.56	98,500	28,500	22,800	Not determined	19,900	Not determined	24,200
0.68	121,000	31,400	22,800	21,300	19,900	19,900	24,200

(1) Low-alloy structural steel, minimum tensile strength 73,000 psi.
(2) Analysis not given.
(3) 0.1 C, 0.8 Si, 1.3 Mn, 20 Ni, 25 Cr, 0.019 N_2, 0.040 O_2.
(4) About 1 Cr, 0.2 Mo.

0.27% C (tensile strength, unwelded, 78,000 psi), but the reverse was true for covered electrodes.

Effect of Carbon Content of Base Metal. Lohmann and Schulz[34] (1933)

Carbon Content	Rotating-Beam Fatigue Limit, psi	
	Bare or Cored	Covered Electrodes
0.08%	17,000–20,000	20,000–21,000
0.27%	14,000–17,000	23,000–25,500

Bend fatigue tests on gas- and arc-welded tubes by Müller,[128] Hoffmann[112] and Wegelius[129] also demonstrated that there was scarcely any advantage in raising the carbon content, particularly beyond 0.35% C in plain-carbon and alloy tubes, and that the scatter in fatigue results increased with carbon content. Schulz and Buchholtz[18] state that the direct-tension fatigue value is generally lower in steels with 0.25% C than in steels with 0.10–0.15% C. Welds in steels with 0.24 % C have 20 to 30% lower fatigue values in direct tension than steels with 0.16% C and the same static tensile strength (procured by alloying). The effect of the carbon content of the filler rod on the rotating bend value of machined 60° double-V welds in plate containing 0.1% C was studied by Becker[22] using the Lehr short-cycle method. Although the filler rods contained 0.1 to 0.32 % C all welds had about the same carbon content (0.04–0.06). For gas welds both as-welded and after forging (40% reduction at 1050–950° C.) there was an increase of 20% in fatigue value as the carbon content of the filler rod was raised to 0.32%, but in D.C. arc and atomic hydrogen welds there was no clearly defined effect. Becker surmised, without analytical data, that the beneficial effect of increased carbon content in the electrode was associated with a corresponding decrease in oxygen content in the weld.

There is obviously a need for a comprehensive fatigue study of welds with various known carbon contents both as-welded and after mechanical and thermal treatment. At present the inability of carbon to raise the fatigue value in welds appears to be attributed to the greater air-hardening capacity of the higher carbon steels with consequent development of micro-cracks, and to the greater sensitivity of the higher-carbon steels to stress concentrations occasioned by their generally lower weldability and greater porosity.

Alloys

Low-Alloy, High-Strength Structural Steels

The fatigue value of welds in low-alloy, high-strength structural steels of the types containing Mn, Si, Cu or Cr has been investigated mainly in direct stress fatigue using pulsators. These results are the basis of the German Plate-Girder Railway Bridge Specifications which are dealt with later. Graf[6] found pulsating tension fatigue strengths from 21,400 to 31,300 psi in arc-welded, unmachined butt welds in low-alloy steel (composition not stated; maximum tensile strength 74,000 psi, known as St 52, and generally containing up to 1.5 Mn, 0.2 Mo, 0.7 Cr, 0.7 Cu or 0.5 Si). Kommerell[130] gives 12,800 psi to 25,600 as the pulsating tension fatigue values of mild steel butt welds and 21,400 to 25,600 for St 52. In neither case was process (gas or arc) or type of filler rod of any importance, although there was less scatter in the results with gas welds. The maximum values found in any specimen were 27,000 psi for mild steel and 31,300 psi in St 52. The latter value is obtained in mild steel butt welds if the weld is inclined 45° to the axis of loading, and is exceeded by machined mild steel butt welds (34,100 psi).

These tests, which were carried out with $^1/_4$-inch plate, showed that welds in the low-alloy steels had acceptable fatigue value but that they possessed little advantage over mild steel in fatigue except at high values of superimposed tension, as Schaechterle's diagram shows, Fig. 13. Graf[6] demonstrated that the same was true for fillet welds, and other investigators are of the same opinion. The better performance of low-alloy structural steel in the highly prestressed condition is due to its high yield point, but, as Bierett[12] found, butt welds in St 52 parallel to the axis of tension have practically the same pulsating tensile fatigue value as in mild steel (cored or coated electrode) except above about 50,000 psi superimposed tension where St 52 is superior.

In rotating-bend fatigue, Lohmann[34] found that butt welds in mild steel and St 52 (0.18 C, 0.7 Cr, 0.4 Cu, 1.0 Mn) with bare and cored electrodes had the same fatigue limit, but with covered electrodes St 52 gave 25,000 psi as compared with 20,000 for mild steel (0.08 % C, 0.5 Mn). In alternating and pulsating bend, annealed butt welds in mild steel and St 52 had about the same fatigue limit: ±28,500 psi (alternating bend value of machined specimens). Gerritsen and Schoenmaker[75]

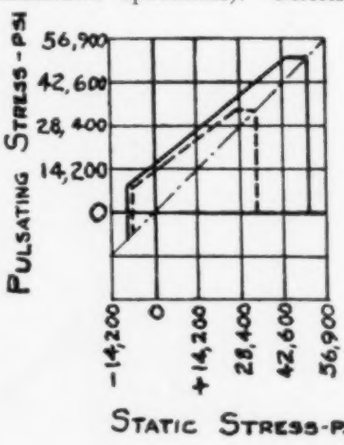


Fig. 13—Direct Tension Fatigue Strength of Arc and Gas Butt Welds, Unmachined Full Line—Low-Alloy Structural Steel (Tensile Strength = 74,000 psi)-Dashed Line—Mild Steel (Tensile Strength = 53,000 psi). K. Schaechterle[121]

also found no difference in fatigue limit in rotating bending between mild steel and St 52 as the following table shows. The electrode used for St 52 contained 0.35 Mn, 0.50 Cr, 0.30 Cu. An all-weld-metal specimen therefrom had a fatigue limit of 30,600 psi. The composition of the electrode for mild steel was not stated. The specimens were turned from V butt welds in 1-in. plate.

Rotating Bend Tests. Gerritsen and Schoenmaker[75] (1933)

Steel	Endurance Limit, psi
1.0–1.3 Mn, 0.15–0.25 Mo, 0.35 Cu	30,600
0.7–1.0 Mn, 0.4–0.6 Cr, 0.6–1.0 Cu	30,200
1.2–1.6 Mn, 0.3–0.6 Cu	27,200
Mild Steel	30,200

Thierens,[131] and Schoenmaker,[132] however, found that welded St 52 had considerably higher rotating- and reversed-bend and torsion fatigue limits than mild steel but that the fatigue values for St 52 were lowered 5 to 30% by welding, but in mild steel welding did not lower the fatigue limits (see Appendix B, Tables 2 and 4). Kater[133] also found that welded St 52, in the form of unstiffened I beams, had a higher fatigue value 37,000 psi upper stress; 17,000 psi lower stress than welded mild steel 31,000 psi upper stress; 16,000 psi lower stress; these values represent the maximum and mean stresses in the extreme fibers, not in the welds.

The reversed-bend fatigue values of several high-strength structural steels have been determined by Orr,[71] on double-V welds (electrode not stated). His

Reversed-Bend Value of Unmachined Arc Welds. Orr[71] (1935)

Material	Composition	Endurance Limit, psi Stanton and Pannell Method Plate	Un-machined	Machined Flush
Mild Steel	.....	*A* 26,900	21,300	21,300
Alloy Steel	0.25 C, 1.5 Mn	*A* 35,600	21,700	24,900
		B 35,400	22,000	...
Alloy Steel	0.28 C, 1.0 Mn, 0.5 Cu	*A* 37,600	22,200	...
		B 36,800	26,000	...
Alloy Steel	0.28 C, 0.8 Mn, 0.5 Cu, 0.6 Cr	*A* 38,600	23,700	...
		B 36,500	25,300	...
Alloy Ateel	0.35 C, 1.1 Mn, 0.6 Cr	*A* 38,600	25,600	29,800

A—As-welded.
B—Stress-annealed 600° C., ½ hour, furnace cool.

results are shown in the above table. The static tensile strength of unmachined welds in medium-carbon, alloy plates was up to 50% higher than that of mild steel, but the maximum improvement in fatigue limit was only 22%.

It is shown in an earlier section that the relatively low fatigue values of low-alloy structural steels as compared with mild steel is due to the relatively greater effect of notches and stress raisers in the higher strength plates. Schulz and Buchholtz[18] found that models of welds with 2 mm. high reinforcement machined from solid mild steel or low-alloy structural steel gave 21,400 and 24,200 psi, respectively, in alternating direct tension-compression and that both steels gave 40,000 psi in pulsating tension. These values are about 30% lower than the fatigue limits for flat plate and represent the maximum fatigue values that can be obtained in perfect, unmachined welds. The average fatigue value of low-alloy structural steel containing a hole in pulsating tension is 28,500, for mild steel 25,600 psi, according to Graf.

Recently, Diepschlag, Matting and Oldenburg[94] have offered a general theory of the fatigue strength of welds

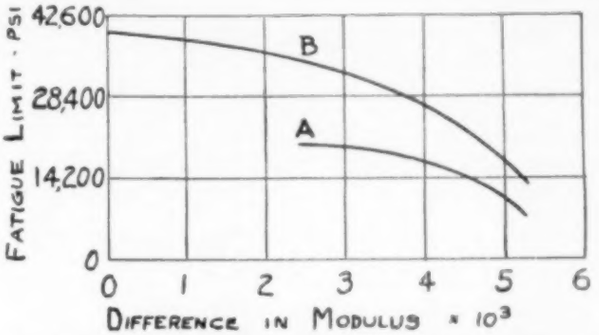


Fig. 14—Relation Between Pulsating Tension Fatigue Limit (2 × 10⁶ Pulsations) and the Difference in Inverse Modulus of Elasticity (Expressed as (1/E) × 10⁶ Kg.⁻¹ Mm.²) Between Weld Metal and Parent Steel. Curve A—Cruciform Specimens. Curve B—Butt Welds. Diepschlag, Matting & Oldenburg[94]

in low-alloy steels based on the difference in modulus of elasticity between weld metal and plate, Fig. 14. The figure is based on V, X and double-T welds in two high strength plates (0.13 C, 0.61 Si, 0.95 Mn, 0.46 Cu, 0.17 Mo; and 0.18 C, 0.38 Si, 0.72 Mn, 0.26 Cr, 0.51 Cu, 0.07 Mo) using four coated electrodes with different contents of Mn, Cu and Cr, and one gas rod containing 3.3% Ni. The pulsating fatigue strength thus appears to be closely related to differences in modulus of elasticity, but is not at all connected with the notch impact value of the weld or the static strength of filler rod. It was also shown by pulsating tension fatigue tests on flash welds between various steels that the fatigue value of a flash weld between two different steels is less than the fatigue value of the weaker partner. The larger the difference in modulus of elasticity between the pair of steels, the greater is the percentage decrease in fatigue value of the weld below that of the weaker steel. Matting and Otte[274] (see Table I, Appendix B), consider that the theory is strengthened by the fact that high-quality gas welds had 10 to 15% higher fatigue value than coated electrode welds of identical microstructure. The more gradual transition from base metal to weld in the gas welds is said to account for the difference in fatigue. No allowance was made, however, for the peening, and special treatment of the gas welds. It is concluded that, for best fatigue behavior, weld metal and plate should have as nearly as possible identical elastic moduli in order to minimize shear forces and stress peaks caused by cross-sectional contraction.

Although it may be found that this theory (internal-notch theory) has only a limited application, the opinion has often been expressed that optimum fatigue behavior is obtained by a weld metal similar to the plate. Thus, Orr[71] explains his own results and reconciles them with Brown,[106] who found that the alternating direct stress fatigue value of a series of welds in mild steel with different electrodes was in inverse proportion to the static strength of all-weld-metal deposited by the electrodes. Jennings[134] also found that the rotating bend fatigue limit of a hot-rolled steel to cast steel weld (bare or coated electrode) was lower than either the welded cast steel or welded hot-rolled steel. Sulzer,[65] on the other hand, showed that a T formed by welding a steel casting to boiler plate had about 70% higher reversed-bend fatigue value that a boiler plate-to-boiler plate weld. The high fatigue value of austenitic welds, determined by Schick[10] and Kautz[55] also appears difficult to reconcile with the elastic-modulus theory.

Manganese as an alloying element in filler rod (up to 3.15% Mn) and weld (up to 2.4% Mn) with the content of other elements held constant was shown by Becker[27]

to have very little effect on rotating-bend fatigue value as determined by the Lehr short-cycle method. Becker used the D.C. arc with bare electrodes, gas and atomic hydrogen processes.

The question of suitable alloy combinations in structural steel from the standpoint of the fatigue value of welds has been raised by surprisingly few investigators. Manganese, state Schulz and Buchholtz,[18] should be below 1.2% in high-strength structural steels, but silicon and copper are not unfavorable to fatigue strength. This is in agreement with the belief that high-strength structural steels sometimes tend to air harden after welding, particularly in light sections and after arc welding; silicon or copper, on this account, would seem to be better additions than chromium or manganese.

Other Alloy Steels

The fatigue value of V butt welds by the atomic hydrogen process in plate containing 0.28–0.35 C, 0.5 Mn, 1.1 Cr, 2.0 Ni, 0.25–0.40 Mo, has been determined by Weinman[19] using the cantilever machine and three alloy filler rods. A rod containing 0.47 C, 1.98 Si, gave the highest fatigue value in the weld (25,000–35,000 psi) but in the form of all-weld-metal this rod was inferior to a rod containing 0.46 C and 3.4 Ni (35,000–40,000 psi). However, Weinman concludes that fatigue value in general is a function of the composition of filler rod not plate metal. Thornton,[92] also using the cantilever machine, showed that chromium-vanadium and carbon-vanadium welding rods gave higher fatigue values in gas welded boiler plate than low-carbon rods.

The comparative fatigue value of chromium-molybdenum electrodes was higher than chromium-nickel or $3^1/_2$% Ni electrodes in plate containing 0.32 C, 3.4 Ni, according to McManus[135] using the Upton-Lewis reversed-bend machine. Barnes[96] using this type of machine, showed that, as a rule, welds in plate containing $3^1/_2$% Ni withstood 20 times as many cycles at 30,000 psi as plain medium-carbon plate, a low-carbon electrode (0.13–0.18 % C) being superior to chromium-vanadium (0.89 Cr, 0.15 V) or nickel-chromium (1.0 Ni, 0.5 Cr) electrodes (bare and wash-coated) for both plates.

Austenitic Steels

The rotating-bend corrosion fatigue limit of welds in 18–8 Rezistal *KA2* plate and rod (0.07 % C) is reported by Harvey and co-workers (see section on Corrosion Fatigue). The fatigue strength of spot-welded V2A (18–8), thin sheet, is 11,400 psi, according to unsigned German results. The same strength was found in gas-welded and arc-welded plates 0.2 in. thick. The tensile strength of the welded sheet was 128,000 psi; of the unwelded sheet 213,000 psi. The fatigue limit of spot-welded 18–8 is estimated by Hoffmann[93] to be 26,000 to 31,000 psi, and of an 18–8 containing 0.1 C, 1.3 Ta to be 37,000 psi. According to Rigby,[297] 14% Mn steel bars welded with patented electrodes, tested under alternating impact and bending stresses, required 7500 alternations to fracture. Welds made with ordinary 14% Mn steel withstood 600 alternations.

The fatigue value of austenitic welds in mild steel plate has been determined by Kautz,[55] Schick[10] and Schönrock.[136] Krupp's Nichrotherm patented austenitic electrode containing about 0.1 C, 0.8 Si, 1.3 Mn, 20 Ni, 25 Cr, 0.019 N_2 and 0.04 O_2 was used. Kautz gives 0.2 C, 18.4 Ni, 22.3 Cr, 0.044 N_2 and 0.05 O_2 as a typical weld analysis in Izett plate and found that the pulsating tension fatigue strength of such unmachined welds was 24,200 psi, of the machined welds, 28,800 psi. The pulsating tension fatigue strength of Nichrotherm welds in low-alloy structural steel (composition and type of specimen not stated) is given by Schönrock as 25,600 to 27,000 psi as compared with 22,800 to 24,200 psi for the same plate welded with a light-coated low-alloy electrode. Machined V welds in mild steel using austenitic electrodes have a pulsating direct-tension fatigue limit, according to Schick, of 34,200 psi upper stress; 17,000 psi lower stress when the breadth of the specimen is $3^1/_2$ inches, but only 27,000 psi upper stress; 5200 psi lower stress when the specimen is $1^1/_2$ inches wide; plate thickness in both is about $^5/_8$ inch. No explanation was offered. About the same fatigue strength is developed in austenitic welds in Izett plate.

Cast Steel and Cast Iron

The rotating-bend fatigue limit of cast steel welds is 15,800 psi (bare electrode, cast kerf, 0.28 C, 0.86 Mn, 0.47 Si) according to Jennings.[134] For welds in cast steel (0.31–0.39 C) made with fluxed and bare electrodes, Sampson[304] found an endurance limit of 20,000 psi (type of test not stated). Jennings[134] and Sulzer[65] (Appendix B, T joints) give fatigue values (23,200 psi, fluxed electrodes[134]) for steel casting to rolled steel for welds.

The rotating-bend fatigue limit of cast iron (3.12% total C, 2.34% graphite, 2.65 Si, 1.05 Mn, 0.37 P, 0.027 S, 0.06 Cu) with and without the cast skin, which had a very fine graphite eutectic structure, has been investigated by Bartels[38] using short-cycle as well as the usual Wöhler methods. The ends of 0.79-inch bars were turned to 45° cones and cold-welded by gas with a cast-iron rod (3.4 C, 3.15 Si, 0.9 Mn, 0.7 P, trace S) using a flux. The specimens with skin were vertically cast and were welded in a jig to hold the bars concentric. The results are given in the following table:

Rotating-Bend Fatigue Limits of Welded Cast Iron. Bartels,[38] 1930

	Tensile Strength		Endurance Limit (10×10^6 Cycles) psi		
Cast Iron	Unwelded	Welded	Unwelded	Welded	Welded Annealed
Without Skin	27,000	10,000	10,700	8,500	8,500
With Skin	38,400	35,200	28,500	25,600	22,800

The annealed specimens were brought to a yellow heat with two torches, held several minutes, and cooled in sand, or held $^3/_4$ hour at 950–1000° C. in an electric furnace. Bartels concludes that the decrease in fatigue strength by welding is proportionately less than the decrease in tensile strength, for specimens without skin, and that annealing is not beneficial to fatigue properties. A few tests on specimens containing two welds in the stressed length gave higher fatigue strengths than single welds.

The cantilever fatigue limit of gas welds, 45° V, in 1-inch cast iron (3.46 total C, 0.74 combined C, 1.33 Si, 0.106 S, 0.66 Mn, 0.282 P) using cast-iron welding rods was 12,000 psi; the unwelded cast iron gave 13,500, according to Mochel.[138]

Brazing

Pulsator tests on two "Sildo" brazed specimens (composition not stated) of mild steel, $1^1/_2$ in. $\times$ $^1/_4$ in. cross section, 10^6 cycles, gave 28,500 psi and 32,200 psi as the pulsating tension fatigue strength by the step-up method, as Keel[139] has shown. He also found that the reversed bend fatigue limit of the brazed joint, $^3/_4$ in. $\times$ $^3/_8$ in. cross section was 20,000 psi. McManus[135] states that a brazed joint (aluminum bronze) in steel containing 0.32

C, 3.4 Ni, is inferior in reversed bending, Upton-Lewis machine, than welded joints using alloy steel electrodes. Sambraus[306] concluded from an investigation of the pulsating tension fatigue value of soldered joints (88 Cd–12 Zn) in steel that the allowable shear stress in fatigue is 3550 psi. The steel strips were 0.31 inch wide, 0.016 inch thick; a joint with 0.16 inch overlap, 0.001 inch solder thickness, 0.046 sq. in. soldered area withstood 8 million reversals (upper stress 5150, lower stress 3400 psi) before fracture. Lead-tin solder (60 Sn, 40 Pb) was considerably inferior.

Non-Ferrous Metals

Only one investigator, Bartels,[38] has made an extensive study of the fatigue properties of welds in non-ferrous metals and alloys. Bartels determined the rotating-beam fatigue limit of copper (99.62% Cu using copper welding wire and a flux), aluminum (weld analysis: 98.6 Al, 0.7 Cu, 0.2 Fe), silumin (weld analysis: 10.14 Si, 0.46 Cu, 0.70 Fe), and copper-silumin (weld analysis: 9.80 Si, 1.03 Cu, 0.77 Fe; pure silumin rod). All specimens were cold-welded with acetylene. The results are summarized in the following table.

Rotating-Bend Fatigue Limits of Non-Ferrous Welds. Bartels[38] (1930)

Material	Tensile Strength (psi)		Endurance Limits (10×10^6 Cycles)		
	Unwelded	Welded	Unwelded	Welded	Welded Annealed
Copper	39,000	17,700	12,100	5,700	6400
Aluminum	17,400	13,400	8,500	8,500	..
Silumin	19,500	6,250	7,800	10,700	5000
Copper-Silumin	16,600	10,100	9,300	11,400	..

The copper welds were peened at a red heat; the annealed specimens were heated to a bright red for ten minutes and cooled in sand. The aluminum welds were also peened, but the cast silumin welds were not; the annealed specimens were torch heated for about ten minutes and cooled in sand. The torch-annealed copper-silumin welds were so brittle that they broke during machining. Understressing raised the apparent fatigue limit in both the aluminum and the silumin welds, and a specimen of silumin containing two welds in its stressed length had a fatigue limit of 12,100 psi.

The reversed-bend fatigue limit of welded copper, aluminum and Aldrey wires (about 0.1 inch diam.) has been determined by Friedmann[79] whose results are given in the following table. The problem of preventing fracture in the grips of the Föppl-Heydekampf machine, which applies a uniform bending moment over the entire specimen, was solved by using cardboard packing and cold-rolling the ends of the wires in and near the grips. The heat of welding softened the hard-drawn aluminum wire and heat-treated Aldrey so that fracture occurred in the weld even after filing. After being hammered, the Aldrey specimens usually broke in the base metal.

Reversed-Bend Fatigue Limits of Welded Non-Ferrous Wire. Friedmann[79] (1935)

Material	Fatigue Limit (Welded) S_f (2×10^6 cycles)	S_f (Welded) / S_f (Unwelded)
Soft Copper	9,500–10,200	0.50–0.57
Aluminum (99.5% Al) Filed	<5,500– 6,600	0.50–0.59
Aldrey 0.4–0.7 Si, 0.3–0.5 Mg Filed	8,100–12,200	0.51–0.77
Aldrey Welded, Filed, Hammered	10,700–12,400	0.67–0.78

Aside from the fatigue value of gas-welded copper given by Laute (see the following section on *Corrosion-Fatigue*) and a note by Horn[140] on the fatigue value of cupro-nickel, no other information on the fatigue strength of welded non-ferrous alloys appears to be available. Horn found that the reversed bend fatigue limit of cupro-nickel (20–30% Ni) welded with a weak flame was 16,500 psi but was only 13,000 psi when welded with the type of flame usual for steel; the filler rod had the same composition as the plate, which had a fatigue limit of 23,400 psi.

Corrosion Fatigue

The rotating bend fatigue strength of machined welds in tap water at room temperature has been studied by Harvey and co-workers,[25] whose results are summarized in the table below.

The results show that the rotating bend fatigue limit (R. R. Moore fatigue specimen, 0.3 inch diameter) of welds in mild steel in tap water is generally higher than in air. The heat treatment of the welded 18–8 specimens was not the same for the different welding processes. Harvey concluded that when the welded joint has a higher corrosion fatigue limit than base metal the weld is cathodic and is not attacked by the corrosive agent. Grain size did not appear to be a factor in corrosion fatigue.

Corrosion Fatigue Results. Harvey and Co-workers[25] (1932 and 1935)

Material	Welding Process	Rotating Bend Fatigue Limit, psi (70×10^6 Reversals) In Air (G. E. Thornton)	In Tap Water
Firebox Steel About 0.20 C	Unwelded	32,500	14,000 (McAdam)
Firebox Steel About 0.20 C	Flash	25,000	24,000
Firebox Steel About 0.20 C	Oxyacetylene	12,500 (low-carbon rod)	19,000
Firebox Steel About 0.20 C	Atomic hydrogen	12,500	17,000–18,000
Firebox Steel About 0.20 C	Covered electrode	...	14,000
Firebox Steel About 0.20 C	Bare electrode	7,500 (X weld)	9,000
18–8 (0.07 C)	Unwelded	28,000–29,000	34,000
18–8 (0.07 C)	Flash	.....	40,000
18–8 (0.07 C)	Oxyacetylene	.....	10,000–12,000
18–8 (0.07 C)	Atomic-hydrogen	.....	29,000

Using a high-frequency direct tension-compression machine (30,000 cycles per minute), Laute[141] obtained the following values for welded and soldered mild steel

Corrosion Fatigue Results (Laute[141] 1934)

Material	Condition	Fatigue Limit, psi Air	Water
Mild Steel	Hot-Rolled	24,100	9,200
Mild Steel	Arc-Welded	14,500	9,500
Mild Steel	Soldered	14,200	9,900
Copper	Cold-Drawn	15,600	15,600
Copper	Annealed, Coarse Grain	9,400	9,400
Copper	Gas-welded	3,000	2,400
Copper	Soldered	5,300	5,800

and electrolytic copper. The criterion of fatigue limit was 100×10^6 cycles; at this stage all the Wöhler curves had become horizontal except the curve for unwelded mild steel in tap water. The results with mild steel agree with Harvey's in that welding actually improves the corrosion fatigue resistance. It is doubtful therefore whether the explanation offered by Roš and Eichinger[7] is correct, namely that poor penetration and other defects, in welds as in cast iron, are far more important than small superficial defects, such as corrosion pits. The effect of corrosion on the fatigue value of welded copper is negligible, the low values being due to coarse grain structure. I. B. McCorkle (private communication) points out that corrosion fatigue results depend to some extent on frequency of stress reversal, particularly at high frequencies, such as those used by Laute.

The reversed-bend fatigue limit of a brazed joint in mild steel was found by Keel[139] to be unaffected by tap water. The fatigue limit was close to 20,000 psi both in air and tap water, but the results of only one specimen are reported and the step-up method of loading was adopted. A reversed-bend fatigue machine for testing large specimens of riveted and welded boiler plate in hot and cold corrosive agents has been described by Gough and Clenshaw[142] but their tests are still in progress.

Methods of Design

Methods of designing welded structures on the basis of fatigue have been discussed on a number of occasions, especially during the past few years, and have been embodied in the National standards of Germany and Austria and in important specifications in Switzerland and the U. S. A. These methods of design are complicated and will be only briefly summarized in the following paragraphs.

Germany

Although, strictly speaking, the national standards of Germany (e.g. DIN 4100: Specifications for Welded Steel Structures, 1934) give no design information, the new Specifications for Welded Plate—Girder Railway Bridges[143] adopted in August 1935 by the German State Railways, fully outline the methods of design, and, in addition, require that electrodes pass fatigue tests. The specifications apply not only to plate-girder railway bridges (not highway bridges) but to traveling platforms (not traveling cranes) and to turntables. Both mild steel (52,500 psi minimum tensile strength, St 37) and low-alloy high-strength structural steel (74,000 psi min. tensile strength, St 52) are covered by the specification.

In addition to the usual static, bend and notch-impact tests, filler rods for railroad bridges must attain the values shown in table below. These minimum fatigue strengths must be developed at 2×10^6 cycles (lower stress = 1500 psi) in a pulsator test, as determined from a plot of log cycles *vs.* stress. The scarf angle of the transverse weld is that dictated by the type of structure involved. The angle is 90° for the longitudinal specimen and the ratio of weld to total cross section is 0.12. The longitudinal specimen is new and no definite tensile elongation is prescribed; however, the usual bend test must be passed. The specimens are made as good as possible. No other country includes fatigue tests in welding rod specifications. At present it is considered that the machined transverse specification is the easiest to fulfill using cored or coated electrodes and probably also bare. The requirements of the unmachined specimen will force progress in developing electrodes that will yield notch-free welds. The longitudinal specimen depends principally on the weld itself and is the most stringent test. It is not considered a disadvantage if the electrode passes in only one of the two specimens, that is, if two different electrodes must be used on the job.

Fundamentally the same system of computing permissible working stresses is used as in riveted construction. The diagrams showing permissible stresses of welds in all types of fatigue and service, Figs. 15 and 16, are based on the Kuratorium tests[6] discussed in the section on Endurance Limit of Welds, and on the fact that joint quality is variable. Line I (*a*), (*b*) refers to base metal in the welded bridge and is used in calculating cross section, weight and economy of welded construction. For St 37 this line is identical for welded and riveted construction; for St 52 the riveted has higher permissible stresses. All Line II butt welds must be X-rayed. Reverse welding must be done unless it is structurally impossible, and butt welds to joint plates are always machined. Notches must be machined from butt welds to web plates if the range of pulsating stress is greater than 15,900 psi.

The stresses for welds which are not definitely included in Figs. 15 and 16 are calculated by the so-called "Gamma" method,

$$S = (\text{Gamma})\frac{Mc}{I}$$

where M = maximum bending moment (algebraic)

$\frac{I}{c}$ = section modulus

S = stress in weld designed for fatigue, and

Gamma = the fatigue factor.

The algebraic maximum bending moment is that calculated with the numerically largest static plus traffic load including the impact factor but not including fatigue. The values of the Gamma factor are given for all values of min. M/max. M from -1 to $+1$; for example, for St 37 in pulsating tension Gamma = 1.0, but for St 52 in heavy traffic Gamma = 1.944 when min. M = $-$max. M.

Fatigue Tests Specified by German Federal Railways[143] (1935)

Type of Joint	Pulsating Tension Fatigue Strength, psi	
	Mild Steel (Min. Tensile Strength 53,000 psi)	Low-Alloy Structural Steel (Min. Tensile Strength 74,000 psi)
Double V Transverse, Unmachined	19,900	21,300
Double V Transverse, Machined Machining Marks Lie in the Axis of Loading.	24,200	25,600
Double V Longitudinal, Unmachined	24,200	25,600

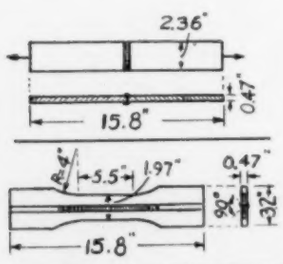
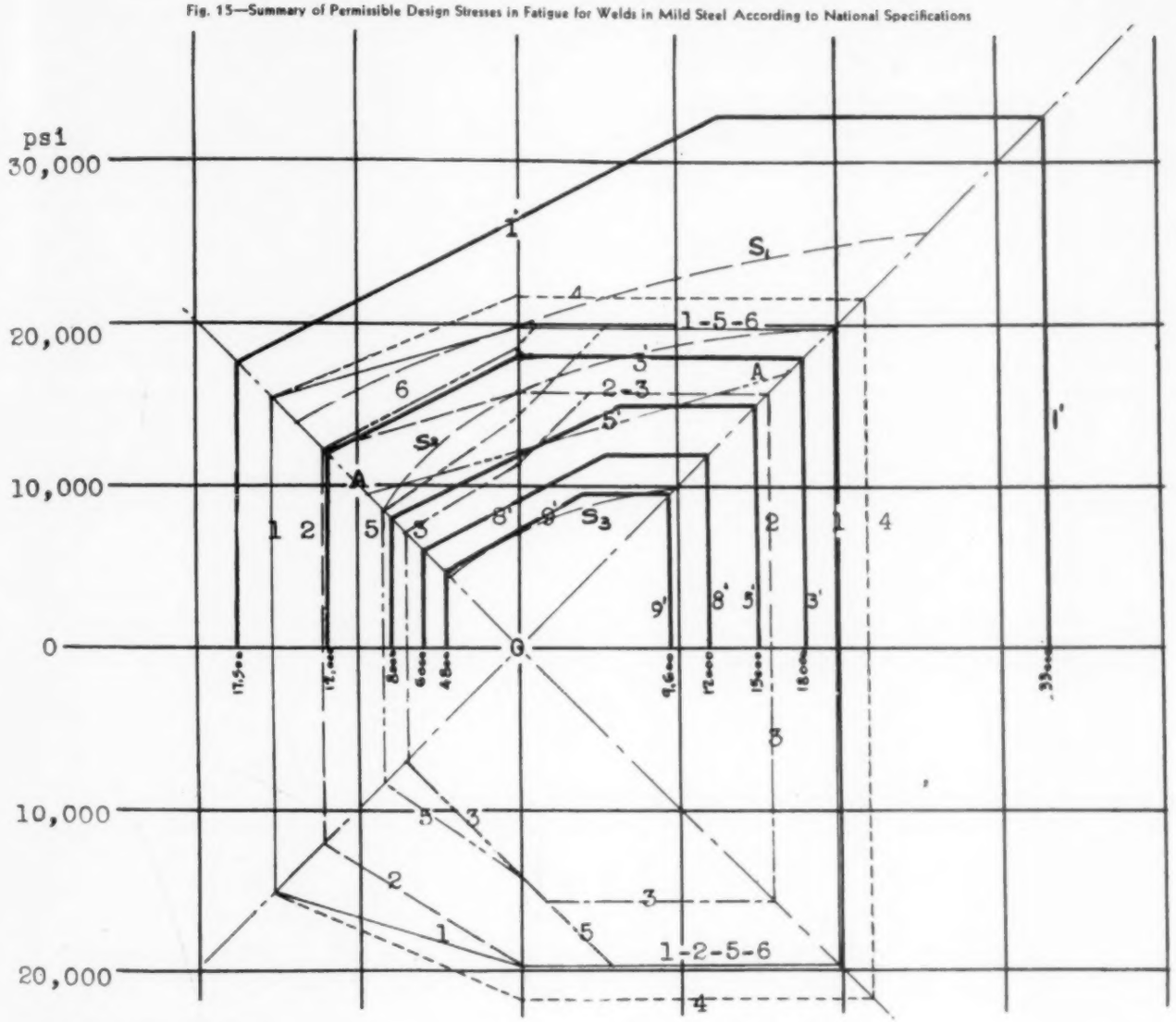


Fig. 15—Summary of Permissible Design Stresses in Fatigue for Welds in Mild Steel According to National Specifications



AMERICAN WELDING SOCIETY
1'—Approx. Experimental Fatigue Strength of Structural Steel
3'—Formula 3, Art. 203 Not Fillet
5'—Formula 5, Art. 203 Fillet
8'—Formula 8, Art. 204 Butt
9'—Formula 9, Art. 204 Fillet
Austrian Standard—*A*
Swiss Federal (B_2 Lines)
S_1 — Butt Welds, Compression
S_2 — Butt Welds, Tension
S_3 — Fillet Welds

German Railways
1—Base Material Unwelded
2—Butt Welds, Reverse Welded and Machined
3—Same as 2, Not Reverse Welded
4—Permissible Principal Stress Given by $S = \frac{S_1}{2} + \frac{1}{2}(S_1^2 + 4T_1^2)^{1/2}$
5—Normal- and Parallel-Shear Fillet Welds Unmachined
6—Same as 5, Ends of Fillets Machined

In order to take account of the fact that the fatigue strength of a weld depends on the type and form of joint and deposit, the stress determined by application of the Gamma factor is further reduced by a shape factor, "Alpha" $S = \frac{(\text{Gamma})Mc}{(\text{Alpha})\ I} \leqq$ permissible stress (19,900 psi for St 37; 29,900 for St 52). A list of 20 values of the alpha factor is given in the specifications for both steels. The fundamental values of alpha for tension (or compression) and shear are 1.0 and 0.8, respectively, for unwelded base metal in the form of beams, etc., and cover plates in both steels. The values of both alpha and gamma factor are derived from Figs. 15 and 16. According to Klöppel,[144] truss bridges are not yet welded on the German Railways on account of the low fatigue strength of fillet welds. Adrian[145] states that certain specifications for Stationary Boilers require welding rods to pass an alternating tension fatigue test as well as notch-impact and age-notch-impact tests. Only transverse specimens of boiler welds are tested in pulsating tension fatigue, according to Fiek.[285] The cross section is usually 1.5 sq. in., the lower stress 1400 psi, and the specimen is tested at 0.9 times the pulsating tension fatigue limit of unwelded plate, noting the number of cycles to fracture. As Vigener[308] points out, the specimen required by the German Boiler Specification is 24 in. long, 8 in. parallel portion, with a cross section of between 2.5 and 3 sq. in. The weld is machined only if it is to be machined in actual service. The specimen is used for evaluating the properties of the weld, not for qualifying the welder.

Switzerland

The enactment[146] of the Swiss federal authorities and the Swiss Association of Engineers and Architects concerning the design, construction and maintenance of structures in steel and reinforced concrete contains design methods for welds based, like the German methods,

on pulsator tests performed in the Swiss government materials testing laboratories. These tests showed that butt welds in mild steel to which the specifications apply had an average pulsating tension fatigue strength (1×10^6 cycles) of 19,900 to 22,800 psi whereas different types of fillet welds gave only 10,000 to 11,400 psi. The diagrams are plotted to the equation

$$S_f = S_s(1 + 0.4\,\frac{A}{B}),$$

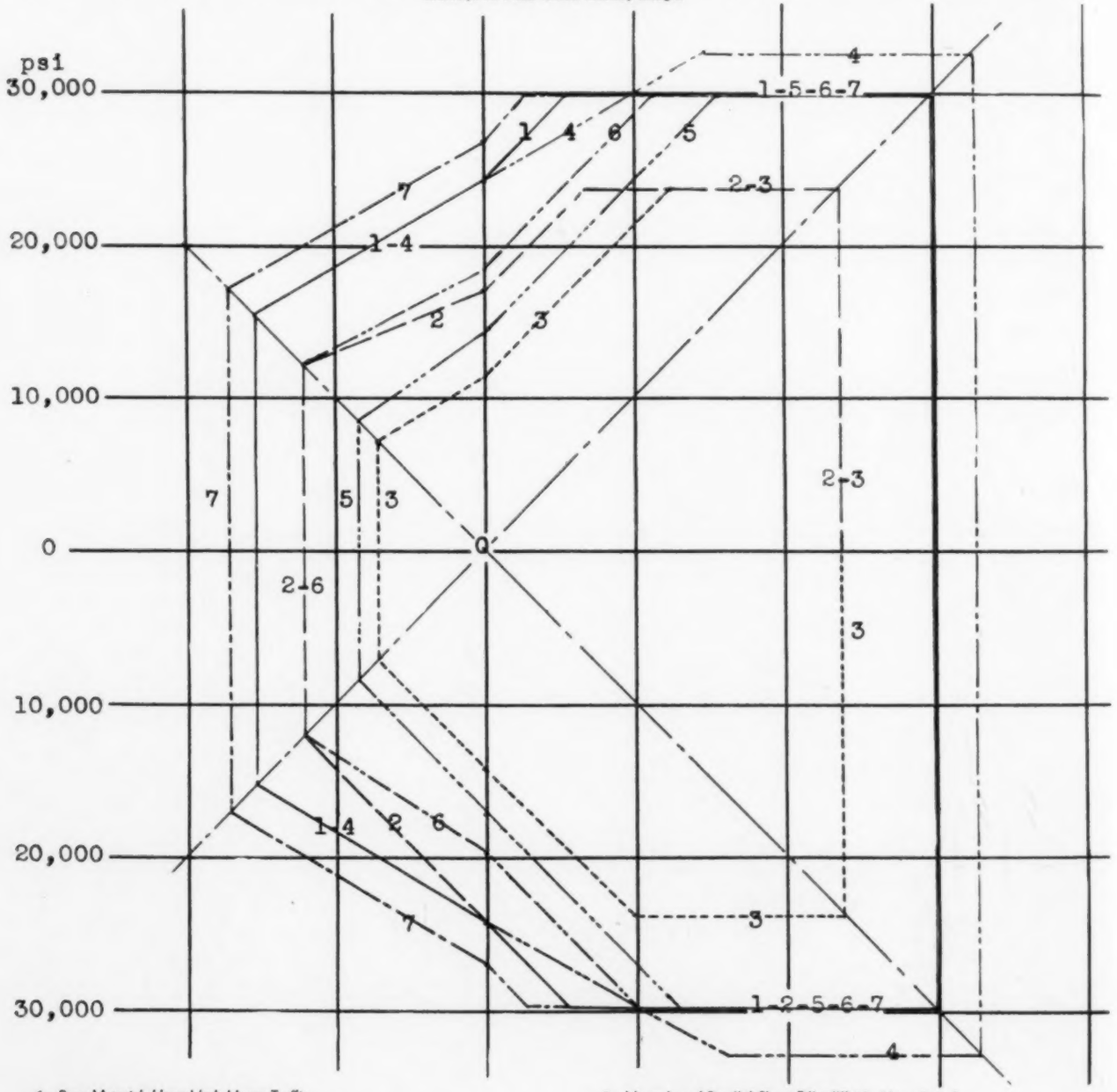
where S_f = stress in weld designed for fatigue,
S_s = stress in weld designed for static load,

and A and B are the minimum and maximum values, respectively, of the forces, moments or stresses with their algebraic sign. Butt welds in compression are assigned the same stresses as unwelded mild steel. This is somewhat in agreement with the German diagrams which show higher pulsating fatigue strengths (0 to max.) in compression than in tension though the respective yield strengths are the same. Butt welds in tension are given only 1/1.4 the stresses allowed in unwelded mild steel. Fillet (normal- or parallel-shear) and butt fillet (double T) welds have only 0.5 the stresses allowed in butt welds stressed in tension. The factor 0.5 compares with the alpha factor 0.65 of the German tables for fillet welds.

Austria

The Austrian specifications for Welded Steel Construction [147] (Önorm *B* 2332, 1934) apply only to mild steel St 37 and to welding rods containing not more than 0.30 C, 0.025 P, 0.035 S and not less than 0.40 Mn.

Fig. 16—Permissible Design Stresses in Fatigue for Welds in Low-Alloy Structural Steel (74,000 psi Minimum Tensile Strength) According to Specifications of German Railways for Plate Girder Railway Bridges



1—Base Material, Unwelded, Heavy Traffic
2—Butt Welds, Reverse Welded and Machined
3—Same as 2, Not Reverse Welded
4—Permissible Principal Stress Given by: $S = \frac{S_1}{2} + \frac{1}{2}(S_1^2 + 4T_1^2)^{1/2}$
5—Normal- and Parallel-Shear Fillet Welds, Unmachined
6—Same as 5, Ends of Fillets Machined
7—Base Material, Unwelded, Light Traffic

Design stresses for welds under fatigue conditions (bending, tension or compression) are calculated by the formula:

$$S_f = 12(1/1 + m\,[1 - B/A])\text{Kg./Mm.}^2,$$

where m is the fatigue factor, equal, for example, to 0.4 for traveling cranes and 0.0 for tanks.

United States

Design stresses for welded highway and railway bridges under fatigue conditions are calculated, according to the specifications of the AMERICAN WELDING SOCIETY (1936), using diagrams similar in conception to the German and Swiss. The specifications refer to the use of covered electrodes only. The method and examples of its application are completely described and illustrated by numerical examples in Appendix A, pages 38 to 44, of the Specifications.

The A. W. S. Bridge Specification permits design stresses in properly made butt welds welded from both sides when subjected to pulsating stresses from zero to maximum of 13,500 psi. When the stresses are alternating, only $^2/_3$ of this value is allowed. There is a 15% penalty in design value in case of single-V backed-up welds. In butt welds subjected to a pulsating shear from zero to maximum, the design value of 9000 psi is allowed, which is again reduced to $^2/_3$ if there is a reversal of stress. The same 15% penalty applies to single-V backed-up welds.

Fillet welds subjected to either tension, compression or shear are allowed 7200 psi when the stress varies from zero to maximum, and $^2/_3$ of this figures when the stress is reversed. Only a good grade of heavily covered electrode is permitted.

The Swedish ship inspection service, according to Ringdahl,[148] requires fatigue tests on 12 specimens, 0.59 inch diameter, cut from a test weld, of which 8 must withstand 5×10^6 cycles at 14,200 psi without fracture.

Dutilleul introduced a rotating bend fatigue test of V welds in the welding specifications of the French Marine Nationale, Port de Brest, 1933.

Other Methods

Although fatigue investigators have often warned that their results were not to be used directly for design (Lohmann,[34] Dorey,[149] Beckmann [150]), there have been a number of efforts besides those listed in the preceding section to embody fatigue strengths in design calculations. Early attempts made by Stone and Ritter,[120] who applied Soderberg's principle to welds, Sandelowsky,[151] Fish[152] and others, and recent suggestions by Jennings,[302] Hovey[153] and Roš and Eichinger[5] were not basically different from the latest methods. All methods apply factors to an assumed or actual fatigue strength, as described in the AMERICAN WELDING SOCIETY Specifications. In welded marine construction, according to Brown,[154] design stresses are computed with a factor of safety of 3 on the endurance limit of the electrode. Hobrock[84] states that in aircraft structures not more than 80% of the endurance limit may be used in dynamic loads, but he notes that the use of a factor applied to static strength is the accepted method at present. Patton and Gorbunow[155] show that section economy can be achieved by basing the section modulus on plastic rather than elastic deformation. They retain the yield strength and customary design factors in their method and suggest that stresses due to the local heat of welding may be neglected, as Kommerell's tests[143] showed.

A well-developed method of welding design has recently been explained by Bobek,[156] who gives an illustrative example of a welded rotor. He points out that in rotor sections, the welds undergo the same fatigue cycles as the shaft due to its rotating bending. To the stresses computed according to standard German practice, Bobek adds 20% to allow for shrinkage stresses and stress concentrations caused by shape, thus obtaining the average stress S_m.

$$S_D = S_m \pm S_A$$

where S_A is the alternating stress. S_D is then multiplied by three factors to obtain the design stress:

b_1, due to shape of seam,
b_2, due to quality of weld, and
b_3, due to the notch effect associated with the type of joint. The factor b_2 is 1.0 for dense, pore-free welds, and 0.5 for usual structural welds. The factor b_3 is given by the VDI fatigue diagrams for the different grades of steel. Bobek[156] presents the tentative values for factor b_1 shown in the table at top of this column. Permissible design stresses in shear fatigue have been determined by von Roessler[209] (see section on Tests of Welded Structures).

The design of welded highway bridges has been discussed by Casper,[284] who points out that the pulsating loads in highway bridges, in contrast to railway bridges, are only a small fraction of the calculated maximum load. Furthermore, the weight of the highway bridge is greater, giving less amplitude of vibration, and the

Fatigue Factors for the Design of Welded Machinery. Bobek[156] (1936)

Type of Joint	Kind of Load	b_1
Double V	all	1
Single V	all	1
Double V with Gap	*A*	0.6
Double V with Gap	*B*	0.8
Double V with Gap	*C*	0.6
Symmetrical Tapered Butt Fillet	all	0.9
Symmetrical Fillet without Taper (Concave)	*A*	0.7
Symmetrical Fillet without Taper (Concave)	*B*	0.9
Symmetrical Fillet without Taper (Concave)	*C*	0.7
Symmetrical Fillet without Taper (Flush)	*A*	0.6
Symmetrical Fillet without Taper (Flush)	*B*	0.8
Symmetrical Fillet without Taper (Flush)	*C*	0.6
One-sided Fillet without Taper (Flush)	*A*	0.4
One-sided Fillet without Taper (Flush)	*B*	0.2
One-sided Fillet without Taper (Flush)	*C*	0.4

A = Tension *B* = Compression
C = Shear, Parallel and Perpendicular to Seam

All welds are assumed root welded, and with combined loads the less favorable value of b_1 is chosen.

cycles of maximum stress are fewer than in railway bridges. On this basis, Casper develops a method of design for longitudinal and transverse girders using combinations of 26-ton steam rollers and 13-ton trucks. A general method cannot be developed for the main members, which depend on the type of bridge under consideration. In contrast to DIN-E4101, Casper believes that stiffeners and connections may be welded to tension members in mild steel without special considerations. In low-alloy structural steel the junction zone of butt welds should be machined only if the stress in base metal exceeds 20,000 psi; the corresponding stress at which machining of end fillets and the ends of side fillets becomes necessary is 24,300 psi. Machining of welds in mild steel is not essential because the difference between allowable stress in static and fatigue loading is not so large as for low-alloy steel. X-raying should be prescribed only for the most highly stressed sections of important welds.

Repeated Impact

The repeated impact test has been rarely used in this country and was discarded many years ago in England. Daeves[157] has a high regard for the test, but Graf (private communication) states that the test is only used for research and its use is declining. A test is known as a repeated impact test whose cycle of load consists of a relatively long rest period and a short load period, the load usually being a falling weight and the maximum stress induced in the specimen being in the neighborhood of the yield point. Such a load cycle is often encountered in machine parts and bolted rail joints, but seldom in structural members. Repeated impact tests on welds are used with three intentions: (1) of measuring a material constant; (2) of determining the general endurance properties of a weld quickly: a quasi-fatigue test; (3) of reproducing service conditions.

The machine for tests of the first type is the Stanton, or a bend-impact machine designed on its principles, such as the Eden-Foster or the Krupp. The specimen for the latter machine is cylindrical and is notched in the center of its span. The impact is applied as a four-point bending load at equal distances from the notch, which usually has a generous radius, about 0.3 in. radius, 0.04 in. deep. The impact load is a weight actuated by an immediate-release cam and the specimen is usually rotated 180° between blows. By not allowing the hammer to strike the critically loaded section, complications due to work hardening are avoided. With heavy impacts the results correlate with the single-blow impact test, but with relatively light impacts requiring thousands of blows to fracture, the results may or may not correlate with the fatigue test depending upon conditions and the material. The number of blows and angle of rotation are stated as the results of the test.

The machine used for reversed bend impact of T welds (Stromberger machine) is cam-actuated, but the impact loads are compressed-air pistons giving 200 cycles per minute. In this, as in the other machines, the stresses due to impact are not known and comparative results are obtainable only on specimens with identical dimensions, especially of the notch. In general, repeated impact tests are more severe than smooth-cycle fatigue tests.

The most comprehensive study of the resistance of welds in mild steel to repeated impact was carried out in 1928 by the British Engine, Boiler, and Electrical Insurance Company.[158] Flush machined specimens were tested in repeated reversed-bend impact (Arnold machine). The ratings of the different welding processes were: coated electrode, 100%, gas (Swedish iron rod)

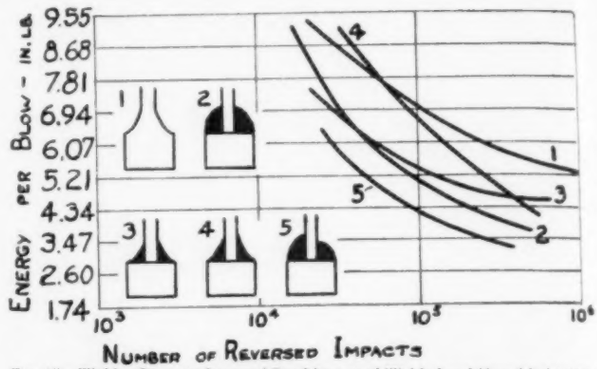


Fig. 17—Wohler Curves in Reversed Bend Impact of Welded and Unwelded Mild Steel T's. Specimen 1 Machined Mild Steel. Specimen 2, Bare Electrode Weld. Specimen 3, Bare Electrode Weld. Concave Contour Produced by Filing. Specimen 4, Covered Electrode Weld. Specimen 5, Bare Electrode Weld, Unequal Height of Weld. Thum & Lipp[15]

40%, carbon arc and bare wire, 10% of the number of blows withstood by the unwelded plate. Welds free from oxides and nitrides gave the best results. Normalizing at 910° C. had little effect.

The harmful effect of nitrides and annealing on double-V arc welds in four grades of structural steel was also observed by Lohmann and Schulz[34] whose results show that for the repeated bend impact test (Krupp machine, round notched specimen), electrode composition and the static tensile strength of the plate are the important factors. Capacity for deformation and fatigue value are not revealed nor is there any relation with the notched or unnotched single-impact test. Best results in plain carbon steels (0.1 or 0.25% C) were obtained with a bare alloyed (0.4 Cr, 0.7 Cu) electrode. Covered electrodes were better for the low-alloy structural steels. In all cases annealing lowered the number of blows (Krupp machine) by 10 to 70%, the reduction being greater the higher the nitrogen content. The best value for any weld was only about 70% of the unwelded plate. However, higher values for arc welds (coated electrodes) than for the unwelded mild steel plate are reported by Passau[159] and by Joellenbeck and Massmann,[160] and Schoenmaker[161] found that welds made with high-quality coated electrodes withstood 85% as many blows as unwelded plate.

The above results on machined specimens leaving the repeated impact question to a large extent open, Thum and Lipp,[15] using a Stromberger machine, investigated the comparative repeated impact value of 14-inch, unmachined T joints made of cast iron or cast steel, or welded (bare electrode) in mild steel. Wöhler curves to 10^6 cycles plotted on the basis of kgcm. per blow became horizontal for the cast steel at 5.4 in. lb., for cast iron at 3.5 in.-lb. but the curve for the welded T was not yet flat at 3.5 in.-lb. The shape of the weld is of vital importance as shown by Fig. 17. Although in specimen 3 about half the weld deposit was removed by filing, the Wöhler curve most nearly approached that of the unwelded specimen. Machined specimens also had smoother fractures, an indication of better dynamic behavior, and gave much less scatter. Concave welds deposited by coated electrodes, specimen 4, showed little ultimate superiority over bare-wire welds.

With a repeated impact machine similar to Krupp's, Sarazin[290] found:

Unwelded mild steel	2129 blows
Welded; notch in middle of weld	1787 blows
Welded; notch 0.32 inch from center of weld	1947 blows

The specimens were rotated 180° after each blow; the notch was 0.04 inch deep, 0.04 inch diameter.

Using a Mohr machine 100 cpm, Tsuruta[287] found that specimens ($^1/_2$ in. diam. at base of notch) of unwelded mild steel failed after 8000 blows, deposited metal 4000 blows (no details).

The repeated impact value of gas welds in mild steel may be lower than in cast iron, as Bartels[38] has shown in the following table. These results are for light impacts (4.4 lb. load, 0.39 inch drop). For heavy impacts (8.8 lb., 0.39 inch) the mild steel welds are superior, however. The surprising fact that a cast-iron weld having less than 10% the single-blow notched-bar impact value of welds in mild steel is more resistant to repeated light impact than the latter seems to be explained only by considering damping capacity. As von Heydekampf has shown, cast iron has a high damping capacity and is consequently dynamically ductile and insensitive to destructive notch and resonance effects. The results suggest that the welding of mild steel with cast iron might involve improved repeated-impact resistance. The exceptionally high values for the welded cast iron may be related to some extent to its fine fracture; the unwelded cast iron had a coarse fracture. Bartels' results on nonferrous welds break new ground, indeed. An unnotched cast Silumin specimen containing two welds withstood 137,000 light blows (4.4 lb.). Bartels also states that unmachined welds were generally more resistant to repeated impact than unwelded specimens in ferrous materials.

Repeated Impact Tests. Machined Specimens. Krupp Machines. Bartels[38] (1930)

Repeated Impact Value, Number of Blows in Thousands

Material	Unwelded	Gas-Welded	Welded, Annealed
Cast Iron without Skin	58	2305	218
Cast Iron with Skin	92	3003*	73
Mild Steel	1300*	1748	854
Copper	165	16	18
Aluminum	14	1	0.6
Silumin	8	11	10

* Unbroken.

Rosenthal[162] has also thrown light on the obscure dynamic characteristics of welds. The repeated impact value of a ductile double T arc weld, he found, was only one-half that of the unwelded mild steel. By cold hammering the junction between plate and reinforcement, he was able to make the weld withstand as many blows as the original plate. It appears, therefore, that Thum's rule, according to which light impacts within the elastic limit raise the fatigue value of the struck part, is valid also for welds under repeated impact. It remains to be seen whether reduction of static ductility and simultaneous increase in dynamic ductility or strength in the cold-worked weld is connected with damping capacity, as in cast iron, or simply with imposed compressive stress. The damping capacity of welds, particularly porous welds, does not seem to have been investigated. The statement by Prox[163] that inclusions and blow-holes have little effect on the repeated impact value of welds may be significant in this connection.

The repeated bend impact test has been applied by Schmit[164] to surfaces built up by welding, by Kilger[278] to flash welds, and by von Roessler[165] to flame-cut surfaces. Schmit found that, for surfacing plate and cast steel with low-carbon steel, gas was superior to the D.C. arc, and that it is the deposit, not the heat-affected zone, that injures the repeated impact resistance. The flame-cut surface, as Roessler shows, is equivalent to a milled, and only about 10% inferior to a planed surface in repeated impact for four types of structural steel. If the machining grooves were at a large angle to axis of impact, or if the flame-cut surface was subsequently ground, the original flame-cut surface had superior repeated impact value. Repeated bend impact tests made under the auspices of the British Acetylene Welding and Consulting Bureau[166] on mild steel specimens $2^1/_2$ in. wide, $^3/_8$ in. thick, showed that welds made in flame-cut scarfs were superior to those deposited in machine-cut scarfs.

Unwelded Plate	7759 blows
Arc-Welded Machine Cut Bevel	8483 blows (average of 5 specimens)
Arc-Welded Flame Cut Bevel	9092 blows (average of 5 specimens)

The weld just protruded from the clamp and the blows were struck 5 in. from the weld at the rate of 600 per minute.

As a laboratory test—for the repeated impact testing machines have an astonishing number of variables, relatively few of which have been investigated—the repeated impact test has undoubtedly yielded important information on properties not prominent in static or smooth-cycle fatigue tests. It must be emphasized that the test will remain empirical in nature until reliable, simple methods for computing impact stresses in the vicinity of notches have been developed. Daeves[157] and Schoenmaker[161] recommend the test whereas Rolfe,[167] Graf and Schuster[168] regard it as unimportant.

The repeated impact test has occasionally been used as a quick substitute for the long-time fatigue test. In view of the fundamental difference between the cycles of loading in the two tests, it is not surprising that such quasi-fatigue tests have been misleading.

The third use to which the repeated impact test has been put is the most frequent, namely: to reproduce service conditions. Whereas in the first type of tests the number of blows usually exceeds several hundreds, the service repeated impact test may use only five or ten blows or may exceed 10^6 depending upon the object of the test. Santilman,[169] for example, reports a six-drop test on a partly-riveted, partly-welded bridge member, Verzillo and Pizzuto[170] describe a shop repeated impact machine for rotating crank axles, and the Rail Joint Committee subjected some joints to over 3×10^6 cycles. Taylor and Jones[171] tested 7-inch welded I beams (intermittent shielded arc welds) made of silicon steel (0.27 C, 0.95 Mn, 0.26 Si) and plain-carbon structural steel in repeated bend impact (38 cpm.) under a punch press. With the welds stressed to 79% of the static tensile strength, the silicon steel beam failed earlier than the plain carbon. In a supplementary test a silicon steel beam withstood 210,000 cycles at 40% of the static tensile strength without failure. Aside from the tests on rail joints perhaps the most informative repeated impact test was that made by Jurczyk[172] on fillet arc welds, a model of such a weld machined from a single piece of steel, and a double-riveted joint. The welded specimens were equivalent to the riveted in repeated tensile impact and were 20% better than the machined models which had equal static tensile strength.

Rail Joints

Tests of the endurance of rail joints are of three types: repeated impact, pulsating stress (smooth load cycle), and service. The most extensive tests of the first type were carried out by the Rail Joint Committee[173] who devised a special machine with an eight-ton anvil for the purpose. The 400-lb. hammer struck the head of the joint centrally; the span was 22 in., height of fall 6 in. and rate 50 to 75 blows per minute. Certain types of seam welded joints gave the best results in this test, but

they were not consistently good; butt, thermit and cast-iron joints followed in order. Using a similar trip-hammer machine (525-lb. hammer, 40 in. between supports, 80 blows per minute, 5 in. drop), Jurczyk[174] found that seam-welded joints made by a patented arc welding process were 10% better than thermit joints, and six times better than the bolted joint. The Krupp machine, on the other hand, gives first choice to flash welded rail steels, thermit joints having only one-third as much repeated impact value, as Reiter[80] showed. Repeated impact tests[299] of arc-welded joints (Katona type) in 85-lb. rails, using a 1102-lb. top falling 43 1/4 inches, span 27 1/2 inches showed that 19 blows were required to cause fracture, the blows being applied to the head of the rail.

The second and less severe type of test: pulsating stress, has largely displaced repeated impact because pulsator results are in terms of a fatigue limit and are not merely comparative. Besides, the smooth load cycle of the pulsator more nearly duplicates service conditions in welded joints, which eliminate hammer and anvil effect.

Stressing joints in reversed bending, Roš and Eichinger[37] determined in 1932 that the thermit joint with a fatigue limit of over ±16,000 psi was superior to their best arc-welded joints which failed below this value. Repeated impact tests, however, rated the joints in the reverse order, which was not unexpected because the first blow in a drop test causes plastic flow and equalizes shrinkage stresses and notch effects.

The majority of fatigue tests on rail joints have been carried out with pulsating tensile stresses in the foot of the rail. The Swiss Federal and Hungarian State Railways regularly perform such tests on proposed methods of welding rail joints. Melhardt[175] gives 19,200 psi as the pulsating tension fatigue limit for seam-welded joints containing 0.1% C and high nickel and manganese. This is somewhat higher than the value 17,000 psi given by Gysen[175] for Thermit joints but lower than that given by Keel[176] for arc-welded joints (27,000 psi). In the last instance the unwelded rail had a much lower fatigue value than the joint. For electric welded rail joints (no details) Gysen found that the fatigue limit was 23,000 psi (private communication from Dr. D. Rosenthal). Melhardt observed that fracture tended to start at the ends of weld beads on the outer edges of the rail foot. Golling and Tulacz[177] report 27,000 psi as the pulsating bending fatigue limit of oxyacetylene butt-welded rail joints reinforced with welded flange straps (filler rod not mentioned). The unwelded rail had a fatigue limit of 43,000 psi. The transition zone is the cause of the low-fatigue value of the welded joints, it is said.

The average joint, according to Keel, withstands 10^6 cycles in 2 to 3 years service, and Csilléry[178] states that, in heavy traffic sections, 3.5×10^6 axles pass over a joint annually. Very few laboratory tests of joints have extended beyond 2×10^6 cycles but the Budapest Tramways state that laboratory tests over long periods give the same information as service tests in track.

On the basis of 12,000 measurements of stresses in welded rail joints in varied traffic Nemesdy-Nemscek[303] found the preceding distribution of maximum stresses around the average.

Distribution of Maximum Stresses Around the Average. Nemesdy-Nemscek[303] (1936)

Stress Expressed as Multiple of Average* of 12,000 Observations	Frequency of Occurrence, %
0.0 to 0.5	10.0
0.5 to 1.0	41.0
1.0 to 1.5	34.3
1.5 to 2.0	11.0
2.0 to 2.5	2.2
2.5 to 3.0	0.5
3.0 to 5.0	1.0

* Average depended on type of rail and was 8500 to 14,000 psi.

Rail joint testing has not yet included the vibration factor which adds only about 10% to the maximum pulsating stress but operates at relatively high frequencies, as Adler[179] has shown.

Creep

The amount of quantitative data on the creep properties of welds is surprisingly small. The limiting creep stresses determined by Stäger and Zschokke[180] are given below (no details):

Limiting Tensile Creep Stress, psi. Stager and Zschokke[180] (1932)

Material	300° C.	400° C.	500° C.	
Mild Steel Plate	31,200	15,600	5700	
All Weld Metal, Gas	18,500	8,500	1400	
All Weld Metal, Arc	19,900	9,900	2800	
Welded Joint, Gas	25,600	12,100	5700	
Welded Joint, Arc	25,600	15,600	5700	
Welded Joint, Arc	...	14,200–15,700	..	(Appaly)

Being a composite of plate and weld metal, the welded joint displays creep properties intermediate between them. Above 400° C. the welded joint is equivalent to mild steel. Keel[181] used the Amsler and the Brown-Boveri creep machines for creep tests on oxyacetylene butt welds in 0.39-in. boiler plate (tensile strength 60,000 psi; weld metal analyzed 0.19 C, 0.80 Mn, 0.30 Si). Plotting stress *vs.* creep velocity and adopting 0.0024% strain per day as criterion, the creep limits at 400 and 500° C. are:

Up to 400° C.—15,400 psi
Up to 500° C.— 4,600 psi

Keel concluded that oxyacetylene butt welds have between 70 and 75% of the creep strength of cast steel of similar analysis.

An intensive study of creep of welds in boiler plate (0.09 C, 0.5 Mn, heavy coated electrodes) at 400° C. by Appaly[182] showed that the creep velocity of a welded joint consisting of six double-V welds in a gage length of 5 1/2 inches is much lower than would be expected by averaging the creep rates for unwelded plate and all-weld-metal. The welded joint has its own specific creep properties probably derived from a combination of microstructure and stress distribution. The creep stress determined by the long-time method (time of test was up to 800 hours, and velocity = 10^{-5}% per hour) was 12,800 psi for plate and 14,200 to 15,700 psi for the weld specimen. After annealing (1/2 hr., 930° C.) the creep stress of the weld specimen was reduced to less than 12,800 psi but the plate did not appear to suffer correspondingly. Stress annealing (1/2 hr., 650° C.) did not lower the creep stress of the weld so greatly as full annealing. The ill effects of annealing are tentatively said to be due to coarse grain structure. Appaly found that Sauerwald and Juretzek's short-time method (creep stress is defined as elbow in plot of log elongation velocity at the end of one hour *vs.* stress) is quite accurate for welds and is superior to Pomp and Ender's method (creep rate between fifth and tenth hour less than 0.003% per hour).

Lea and Parker[21] determined the creep stress giving a creep rate of 1×10^{-5} and 1×10^{-6} inch/inch/hour at the twentieth day at temperatures between 325 and 575° C. using machined all-weld-metal specimens deposited by an electrode analyzing 0.15 C, 0.10 Si, 0.57 Mn and having an iron silicate coating containing manganese. At the critical temperature of water and steam, 374° C., the creep rate (20 days) was less than 1×10^{-6} in./in./hour at 22,400 psi. At 490° C. the corresponding stress

was less than 9000 psi. The creep characteristics of weld metal are not so good as the steel used in superheater tubes (analysis not given) but are excellent except under conditions of superheat. A covered electrode, shielded arc (analysis not given), was also tested but was inferior to the slag-coated.

Creep rates in welded steam station piping at 850° F. (455° C.) determined by the single-step method are reported in the following table by White, Corey and Clark.[187]

Rate of Creep at 850° F., 15,000 psi Tensile.	% per 100,000 Hrs.
Pipe Material (0.33 C, 0.75 Mn, 0.04 Al (Metallic)	1.1
Welded Pipe-to-Pipe	1.2
Welded Pipe-to-Casting (0.24 C, 0.62 Mn, 0.82 Cr, 1.19 Ni, 0.40 Mo)	1.5*

* Reported as 1.3 in Publication B5, Prime Movers Committee, Edison Electric Institute, 1934.

The welds were made by the shielded arc process and were stress relieved (air cooling) by induction heating at 1100° F. The test results were not so consistent for the welds as for the unwelded pipe; the duration of the tests was 500 to 600 hours. At a stress of 12,000 psi there was no appreciable creep at 850° F.

The creep characteristics of gas welds in cold-rolled steel at 360, 550 and 1000° F. (180, 290 and 540° C.) have been studied by Ward[183] who concluded that the welds have excellent creep resistance up to 400° F. In the early stages of creep, welds are not so good as unwelded steel on account of their coarse grain structure. Annealing at 1650° F. (900° C.) is detrimental to the creep properties of welded and unwelded cold-rolled steel. According to Roš and Eichinger[7] the creep limit of plate metal, especially under compressive stresses, may be very materially decreased in the vicinity of welds (no details).

In view of the probable dependence of creep behavior on distribution of stress and shape of specimen, Bugden[184] determined creep data for the design of welded steam piping from actual Dawson joints. The weld (covered electrode) of such a joint in mild steel pipe (0.05% C) withstood a stress due to internal pressure of 87% of the proportional limit for 1460 hours and in addition, a bending moment of 12-ft. tons, which increased the combined stress in the weld to 95.6% of the proportional limit for 1220 hours at 805° F. (430° C.) without objectionable creep. The average diametral creep rate of the weld was about 5 times that of the unwelded pipe, however.

It is F. E. Smith's[185] experience that tubes of hydrogen-resistant steel cannot be joined by any ordinary process of arc welding without seriously affecting creep strength. The resistance butt welding process is therefore used.

Short-time high temperature tensile tests of welds have been reported from a number of sources. In general the welds are not inferior to unwelded material although Ward's results indicated that gas welds in the temperature range 75 to 1450° F. (25 to 790° C.) had only about 70% of the tensile strength of unwelded annealed plate. In the design of boilers Schuster[88] recommends that above 250° C. the rated stress in the weld should be reduced below unwelded plate. There is a serious lack of numerical information on the thermal coefficient of expansion of weld metal, a factor which is especially important for austenitic welds in non-aging steel, as Hessler and Kautz[186] have shown.

Summary of Fatigue Tests on Welded Boilers

Reported by	Dimensions, etc.	Fiber Stresses in Weld, psi Lower	Fiber Stresses in Weld, psi Upper	No. of Cycles	Location of Fracture
Joys & Jasper, 1925	45 In. I. D. 8 Ft. Long, 2 In. Wall, Shock Cycles, Bare Electrode	mainly 11,250	mainly 22,500	7,000	Burst at Static Stress of 32,600 psi in Plate Metal
H. F. Moore, 1929–31	42 In. I. D. 2 In. Wall, Smooth Cycles, Bare Electrode	0	16,500	5,500	Junction Zone of Longitudinal Weld
H. F. Moore, 1929–31	Special Electrode	0	16,500	270,000	Slag in Weld
H. F. Moore, 1929–31	B. & W. Processed Electrode	0	16,500	1,000,000	No Failure
H. F. Moore, 1929–31	B. & W. Processed Electrode	0	16,500	2,000,000	No Failure
H. F. Moore, 1929–31	Same Drum as Preceding	0	22,000	50,000	No Failure
H. F. Moore, 1929–31	Hammer Welded	0	16,500	900,000	No Failure
H. F. Moore, 1929–31	A.S.M.E. St'd. Riveted	0	16,500	1,000,000	No Failure
John Wood Mfg. Co., 1931	Electric Resistance Compression Butt Welds. 12 In. I. D. 48 In. Long, Shock Cycles, 0.101 In. Wall	..	...	Over 300,000	No Failure
H. W. Hawkins, 1932	36 In. I. D., 10 Ft. Long, 3/4 In. Wall, B. & W. Electrode, Shock Cycles	0	20,325	320,000	Short, Fine Crack in Longitudinal Weld
H. W. Hawkins, 1932	Riveted, 1 1/16 In. Wall	0	20,325	320,000	No Failure
M. Ulrich, 1933	26 1/2 In. I. D. 9/16 In. Wall, Pintsch Black Electrode Smooth Cycles	3840	23,800	156,750	Junction of Surfaces of Weld and Plate in Long. Weld
M. Ulrich, 1933	26 1/2 In. I. D. 9/16 In. Wall, Pintsch Black Electrode Smooth Cycles	3840	23,800	98,700	Longitudinal Weld
M. Ulrich, 1933	26 1/2 In. I. D. 9/16 In. Wall, Pintsch Black Electrode Smooth Cycles	3840	23,800	10,000	Longitudinal Weld
M. Ulrich, 1933	As Above but with Cover Plates Welded on After Normalizing	3840	23,800	203,000	Undercut of Normal-Shear Weld of Cover Plate
K. Kautz, 1935	29 In. I. D. 26 In. Long, 1.1 In. Wall, Austenitic Electrode, Shock Cycles	(1400 to 2850)	(19,900 to 23,000)	520,000	No Failure
K. Kautz, 1935	Same Drum as Preceding	(1400 to 2500)	(25,600 to 28,800)	107,000	No Failure
K. Kautz, 1935	Same Drum as Preceding	(2450 to 3800)	(29,200 to 32,300)	36,000	Short Crack (1 Mm. Wide) in Longitudinal Weld
K. Kautz, 1935	26 1/2 In. I. D. 55 In. Long, 9/16 In. Wall, Austenitic Electrode	3980	23,900	400,000	No Failure
K. Kautz, 1935	Same Drum as Preceding	3980	26,800	55,000	Crack in Longitudinal Weld, Ground Out and Re-Welded
K. Kautz, 1935	Same Drum as Preceding	3980	26,800	85,000	Crack in Longitudinal Weld
T. S. Murphy, 1932	34 In. I. D. 72 In. Long; Covered Electrode; Smooth Cycle	0	13,226	10,000	No Failure

Summarizing, the creep strength of welds in mild steel is probably little if any, inferior to unwelded plate up to 500° C. although the initial creep rate may be somewhat higher; full annealing is not beneficial. That there will shortly be more information on the high temperature properties of welds is indicated by the fact that the welding research programs of several technical bodies at the present time include investigations on creep. Since, at the temperature of stress annealing for mild steel, creep is able to eliminate shrinkage stresses. It has been suggested that creep is probably a factor in welds of some nonferrous alloys even at room temperature.

Boilers

A summary of fatigue tests on welded boilers and drums is given in the table on page 29. The cylinder tested by the A. O. Smith Corporation[187] (Joys and Jasper) was a mild steel penstock having four longitudinal welds. The ends of the penstock were closed by 4-in. thick hemispherical shells. The pressure during each cycle was built up slowly and released suddenly, 15 shocks per minute. The fracture of the fatigued drum revealed a very coarse grain structure.

The John Wood Mfg. Company[188] tested four tanks with longitudinal seam welded by electric resistance butt compression method. A quick acting valve applied 150 to 160 psi water pressure 13 times per minute. Two of the tanks withstood over 300,000 cycles; the remaining two withstood 210,000 and 90,000 cycles. None of the tanks was ruptured by the tests (fiber stress and type of material not stated).

The tests reported by H. F. Moore[52] and Hodge[189] embodied a smooth sine-wave cycle of load from zero to maximum, 13 cycles per minute. Over 25 shells were tested of which only a few are listed in the table. The plate for the arc-welded shells was A. S. M. E. code plate (55,000 psi tensile). Details of the Babcock and Wilcox processed electrode are not given. Slag was visible in the fracture of some of the welds. All drums were stress annealed at 650° C. and the reinforcement was ground off.

The mild steel drums tested by Hawkins[190] were subjected to stress cycles of a shock order, 6 cycles per minute, at 20,325 psi in the longitudinal weld; the test pressure was 750 psi. The short fine crack mentioned in the column headed *Location of Fracture* developed in the longitudinal weld at a distance from the circumferential weld. In the subsequent static test the riveted drum started to leak at 27,000 psi fiber stress and the leak developed until pressure could not be maintained at 43,400 psi fiber stress. The welded drum failed at 48,000 psi, fracture starting in the longitudinal weld but terminating in plate metal. Not counting straps and rivets, the welded drum was 42% lighter than the riveted drum of identical capacity.

The mild steel drums tested by Ulrich[191] had two hemispherical ends welded on and were normalized. The cover plates were partly of the Mefi type, partly Höhn type, and were welded on after the drum had been normalized. The stress cycle was smooth (sine-wave type), 15 cycles per minute.

The testing arrangements and results of Kautz[55] are described by him in admirable detail. The test drum was made of Izett non-aging steel (0.25 C, 0.55 Mn, 0.05 Si, 0.01 P, 0.016 S; yield point, 43,500 psi, tensile strength, 71,000 psi). The austenitic electrode (20 Ni, 25 Cr, 0.1 C, 1.3 Mn, 0.8 Si) was deposited in five layers in J-shaped scarfs without backing ring. The stress cycle consisted of a relatively long period at maximum pressure and a short period at minimum, release and admission being sudden, 28 cycles per minute. The dimensions of the drum were autographically recorded throughout the test. The drum was not normalized or stress annealed. Fracture started gradually on the inside of the drum at the junction of the surfaces of plate and bead, and spread outward until it reached the surface. The second of the drums discussed by Kautz had thicker ends ($1^9/_{16}$ in. thick), no manhole, and was made of lower strength steel (Izett I). The stress cycle was of the smooth cycle type, 12 cycles per minute; the drum was not heat treated after welding. The reinforcement was not ground off either drum. The second drum was tested under static pressure at 29,900 psi before the fatigue test. The tests were not carried out at elevated temperatures (e.g., 300° C.) in order to avoid any release of shrinkage stresses. The drum described by Murphy[276] was made of plain 55,000 firebox carbon steel stress relieved 1200° F. After 10,000 cycles at 0 to $1^1/_2$ times working pressure (517 psi based on a factor of safety of 5 and 80% efficiency) the drum withstood a static test to 3450 psi internal pressure followed by an additional 10,000 cycles (12 cpm.) without failure. Varriot[275] describes apparatus for testing welded pressure vessels under pulsating pressure but gives no results.

The boiler fatigue tests, as Dorey[192] points out, show that fatigue failure inevitably occurs in regions of stress concentrations; e.g., gage plugs, manholes and pads (this was true in the tests of Moore and Kautz), rather than in the welded seam itself. According to Schuster,[88] the number of fluctuations of stresses that take place in service, which is greater than that due simply to starting up and shutting down the boiler, is small compared to the large number in fatigue tests. Nevertheless, he and several others at the Welding Symposium of the Iron and Steel Institute suggested that pressure vessels ought to be designed on the basis of fluctuating loads. Kautz emphasizes, however, that the stresses indicated in fatigue tests should not be used for design purposes. The only unsatisfactory welds in all the fatigue tests were those made with bare electrodes.

In 1930 the fatigue value of welded pressure vessels was considered so problematical that the Boiler Code Committee was on the point of inserting a fatigue test in their Code. The test consisted of 10,000 cycles of internal pressure from zero to $1^1/_2$ times working pressure. A number of fatigue tests were carried out by leading pressure vessel manufacturers, such as the Hedges-Walsh-Weidner Company[276] and the Westinghouse Air Brake Company.[277]

Tests of Welded Structures

Laboratory and Workshop Tests

Laboratory fatigue tests on relatively simple welded elements, such as fillet welds and T joints (Thum and Lipp, and others) have been dealt with in the section on Tabulated Results. Fatigue tests of large-size welded structural parts such as I-beams were reported by Bissell[205] and Spindler.[206] Both investigators tested riveted and welded connections between I- or channel sections, and both found that the welded were entirely satisfactory. Bissell subjected 6 in. I-beams welded (butt, bare electrode) or riveted to the web or flanges of a 10-in. I-beam to vibrations (1760 cpm.) set-up by a square cam. After four hours all riveted connections had failed; the welds were in excellent shape after 18 hours. Additional loads had to be applied to cause the welds to fail. Patton and Gorbunow[155] tested simple and three-support unsymmetrical welded beams of mild steel by an unusual method. The beam was subjected to pulsating loads (lever mechanism, time for one cycle not given) in increasing steps until the beam continued to deform after a large number of pulsations (about 1500). The stress

corresponding to this load was always in excellent agreement with the stress computed using a section modulus based on plastic rather than elastic deformations.

Hochheim,[72,100] and Bühler and Buchholtz[17] used the pulsator to test welded I-beams with welded stiffeners above supports and under the applied loads. A plate I-beam with ribbed flanges, and with stiffeners welded to web and both flanges, had a pulsating bending fatigue limit (4-point loading) of 25,600 psi (low-alloy structural steel, minimum tensile strength 74,000 psi). A similar beam with stiffeners welded only to web and compression flange had a pulsating bending fatigue limit of 32,700 psi. Hochheim found that beams with stiffeners welded to web and both flanges, and having a drill hole through the tension flange, withstood 250,000 cycles at 41,200 psi. Graf[207] tested a 10-inch mild steel I-beam butt welded (coated electrodes) at the center to provide a length between supports of 10 feet in four-point pulsating bending. The tension flange of the beam was strengthened by a welded-on plate. With stresses in the tension side of the flange varying from 850 to 22,000 psi, in the compression side from 1400 to 37,000 psi, one beam failed in the tension flange after 1.6×10^6 pulsations, another beam did not fail after 2.1×10^6 pulsations. The test shows that butt welds have considerably higher fatigue value in compression than in tension.

A welded plate girder bridge 30 ft. span, 3700 lb., of low-alloy structural steel (static tensile strength 74,000 psi) using bare electrodes was tested by Bohny[208] by means of a rotating eccentric pulsator (about 540 cpm.). The results are shown in the following table. Fracture occurred almost simultaneously at two places, the main crack occurring at the end of a fillet welded stiffening plate, some distance from the middle of the bridge.

Pulsating Bend Test of a Welded Bridge. Bohny[208] (1936)

Load Step	Amplitude-Inch	Range of Pulsating Stress in Tension Fiber, psi: Middle of Beam Lower	Middle of Beam Upper	At Point of Fracture Lower	At Point of Fracture Upper	No. of Cycles
1	±0.49	4300	17,200	3400	13,700	60,210
2	±0.79	4300	25,000	3400	19,800	69,890
3	±0.98	4300	30,700	3400	22,500	2,250 (fracture)

Since the failed fillet weld had a pulsating fatigue limit of less than 20,000 psi, Bohny concludes that welded stiffening plates should be terminated as near the bearings as possible. Bierett[209,12] reported pulsator tests on butt welded T beams (4-in. flange, 11-in. web) which withstood 2×10^6 cycles at 28,400 psi.

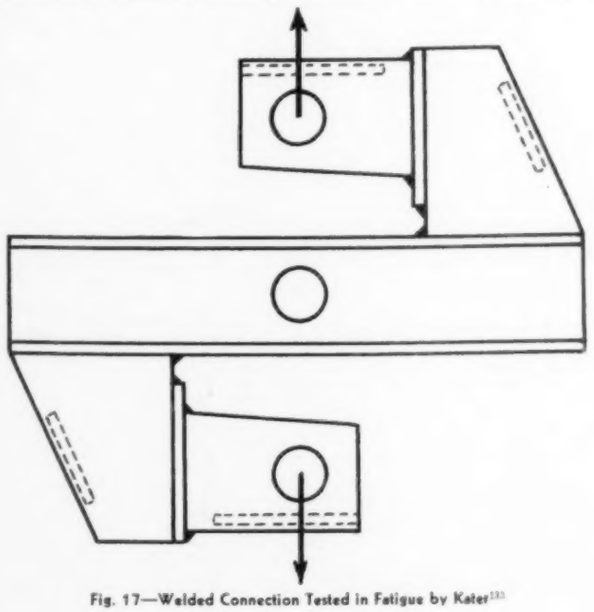


Fig. 17—Welded Connection Tested in Fatigue by Kater[133]

The welded I-beams tested by Kater[133] withstood 3×10^6 cycles at a lower stress of 23,400 psi, upper stress 35,600 psi (low-alloy structural steel, 74,000 psi static tensile strength) and 10.45×10^6 cycles at a lower stress of 13,800 psi, upper stress 25,200 psi (mild steel, 52,500 psi static tensile strength), before fracture (dimensions not given. The two steels gave the following results in the complex all-welded S-shaped structure shown in Fig. 18.

Pulsator Test. Kater[133] (1933)

Steel	Lower Stress	Upper Stress (2×10^6 Cycles)
Mild Steel	15,600 psi	31,200 psi
Low-Alloy Steel	17,100	37,000

The maximum stresses are given in the above table; the welds were not machined. These values are in good agreement with Graf's results for butt welds in the same types of steels. Wilson[76] has reported short quasi-fatigue tests on twelve types of welded girder-to-column connections (coated electrodes). The stress cycle was 25,000 psi tension to 12,000 psi compression approximately at the most highly stressed section. Cracks occurred usually at the weld most highly stressed in tension, but they sometimes originated in the less highly stressed shear fillets of the seat angles. Most of the specimens failed before 20,000 cycles had been reached.

Hochheim[100] also tested a complex all-welded C-shaped structure in the pulsator but gave no results. The correct design for a bent plate with welded or riveted hinges was arrived at by Driessen[47] from pulsator tests. The best design is that in which fatigue cracks just fail to start at the weld-plate surface junction. Schaechterle's observation[21] that service cracks in highly-stressed welded joints in railway bridges occur at the same points as in pulsator tests seems to justify Driessen's procedure of designing complex joints on the basis of experimental fatigue tests. The design of the first welded railway sleeper joints of the German railways was verified[210] by fatigue tests using compressed air hammers, impact pendulums and alternating-bend devices.

Dustin[211] showed the value of pulsating tension tests on models of Vierendeel bridge members (scale 2.5 to 1) in designing for maximum strength and economy. The pulsator tests showed that the maximum stresses did not occur where they were expected. Santilman[169] mentions tests of bridges by rolling stock and of bridge members by repeated impact. Details of a repeated oil pressure test of arc-welded 8-in. expansion joints are given in the Lincoln Prize Papers for 1929. No failure, permanent set, or leaks appeared during the $23^1/_4$ hour test. Alternating stress tests of arc welded elbows under internal pressure, made by the Taylor Forge and Pipe Works[272] showed the vast superiority of welded, over screwed, pipe joints.

Full-size fatigue tests of welded engine bed-plates by Sulzer's works are described by Pemberton.[212] A 10-in. diameter bar supported in the two main bearings of the bed plate was loaded at the midpoint by a hydraulic ram subjected to pressures alternating between zero and 5000 psi (500 cpm.), corresponding to an alternating load of 156,800 lb., the design load being 100,000 lb. The bed plate was sound and free from defects after 37.5×10^6 cycles. Tests on bed plates of different design led to fatigue cracks after a comparatively short time.

Torsion fatigue tests on flanges (plain-carbon steel, 142 Brinell) welded to shafting (plain-carbon steel, 130

Brinell, 2 in. diam.) by means of coated electrodes have been reported by von Roessler[309] in the following table.

Torsion Fatigue Tests of Flange Welds. von Roessler[309] (1936)

Static Tensile Strength of Electrode psi	Type of Weld	Static Torsion Strength of Welded Joint, psi	Torsion (Shear) Fatigue Limit of Joint, psi 0 to Max.	± Max.
60,000	V (Sliding Fit)	49,000	18,000	8500
60,000	V (Loose Fit)	45,000	...	7600
60,000	Fillet Weld	48,000	21,400	9100
75,000	V (Sliding Fit)	57,000	23,200	7800

The throat of the weld was 0.12 inch and the Wöhler curves were horizontal at 2×10^6 cycles. The low strength of the loose fit (0.008 inch play) showed that the sliding fit was partly changed to a press fit by welding stresses. The welds were deposited in a single pass with 0.16 inch electrodes. Fatigue failure always occurred through the throat of the weld, as expected, so that notches in the junction zone were not a factor. Beads with sharp transitions had the same fatigue value as beads with gradual transitions. The fatigue value was, however, sensitive to porosity. Specimens intentionally prepared with low-grade electrodes developed fatigue cracks following the cavities at a relatively low number of cycles. The following permissible stresses in torsion fatigue are suggested for small circular seams, sliding fit, using electrodes with a static tensile strength of 57,000 to 64,000 psi. The surprising discovery that the fatigue limit in non-alternating shear (0 to max.) is more than

Torsion Fatigue Design Stresses. von Roessler[309] (1936)

Type of Stress Cycle	Permissible Design Fatigue Stress, psi 0 to Max.	± Max.
Smooth Cycle	About 8500	About 4300
Weak Impact	About 7100	About 3600
Severe Impact	About 5700	About 2900

twice that in alternating shear (± max.) is not in agreement with the results of Roš and Eichinger (Appendix B, Table 4) and requires confirmation from a larger number of tests.

Service Tests

It is beyond the scope of this review to give a complete account of service results of welds under fatigue conditions, either in the form of welded structures or repairs. Dorey,[193] Burn,[108] and Pohl and Ehrt[56] among others show that welding, unless unintelligently performed, is perfectly reliable. Burn states that the stress-raising and crack-producing capacity of welding is especially exerted in large forgings. According to Salmon and Bernhard,[194] secondary bending moments in open-web girders are considerably increased by welding, and the stiffness of welded frames is unfavorable under fatigue conditions. On the other hand, Bonami and Goelzer[195] state that the rigidity of a welded traveling crane eliminated vibrations and was beneficial to fatigue value (see section on *Bridges*). The incorrect welding of superheater tubes of different thicknesses is given by Pfleiderer[70] as a cause of fatigue failure. Long operation and repeated cleaning so weakened the forge-welded tubes in water-tube boilers that forge welds, usually low-grade, have been forbidden in these boilers in Germany since 1926, according to Krüger.[60]

Service results in marine applications, Pierce[196] stated in 1931, have shown that covered electrode welds are equivalent in fatigue value to rolled plate. After $3^1/_2$ years of service a welded pressure vessel which had been tested at a relatively high stress before being placed in service, was tested to destruction in 1932 by Jasper;[197] the vessel developed full strength, failure occurring in plate metal. Dorey[193] has observed that service fatigue failures of welded water-tube boilers occur at points of stress concentration rather than in the welded seams. Stieler[198] described the perfect record of bare-electrode welded locomotive gangways during over 100,000 miles of service. In 1930, Woofter[199] reported that welded truck rims had given no trouble during 12 to 14 years of service, and Cooke[200] in 1911 stated that oxyacetylene welded goods-wagon frames stood up well in service. Boden[201] and Schinke[202] report excellent success with welded cars on the German Railways and give methods for avoiding "corner" welds, which are weak in fatigue.

Although the above review has shown that welding, even bare-electrode welding, is reliable under fatigue conditions, it is usually difficult accurately to evaluate the number and magnitude of the cycles that a welded part is expected to withstand. As stated in the section on rails, rail joints in average sections undergo 10^6 stress reversals a year. In welded railway bridges, each train (locomotive) is considered to contribute only one cycle; a simple calculation then gives the number of cycles undergone during the anticipated life of the bridge. This was the basis of the choice of 2×10^6 cycles as the criterion of fatigue value in the pulsator tests of the German investigators. Ziem[310] states that welds in cast steel locomotive frames undergo 2.83×10^6 complete reversals of stress (high, but no details) per month without trouble. It is the general opinion that boilers never undergo more than 10^6 cycles of stress during their expected life. Fatigue failures are seldom encountered in aircraft, according to Wagner,[203] and this is repeated by Templin and Tuckerman,[204] who states that except in wires there are very few fatigue failures in the structural parts of aircraft. Airplanes with welded steel fuselages have flown over 6000 hours without trouble with welding.[280] Burges[204] thought it hardly possible that there should have been 10^6 reversals in the girders of the Macon. But, as Hobrock[84] states, "there is no certainty as to the number of stress reversals encountered by structural parts during their life in airplanes and airships." This appears to be true to a great extent in dynamic structural design. Gough[142] recently declared that the most common type of service failure in his experience was that of fatigue.

Riveting and Welding

Strengthening by Welding

Practically the only experimental fatigue study of riveted joints strengthened by welding is that of Kommerell and Bierett.[213] Their results are summarized in the table on the top of the next page.

All combined joints were riveted in the usual way. The joints of series 1 and 3 were loaded about ten times between 7000 psi and 14,400 psi tension (all stresses are related to the weakest cross section through rivet holes). The joints of series 3(*b*) were given 1×10^6 cycles between these limits. The large joints were loaded between about 6000 and 15,000 psi about 10 times; only purely elastic deformations were found after these preloadings which were designed to simulate service loads on a bridge. The specimens, which were of mild steel, were then welded under the lower pre-load with bare electrodes, 170 amps. D.C. The temperature of the rivets did not rise above 200° C. The Losenhausen pulsator (500 cpm.) was used for the small specimens; the pulsating bridge (210 to 230 cpm.) for the larger specimens. The fatigue limit is defined, as usual, as the stress indicated by the Wöhler curve at 2×10^6 cycles, and is quoted on the reduced cross section, although some specimens broke in the original cross section. The Wöhler plots had by no means flattened out at 2×10^6 cycles.

Results of Pulsator Fatigue Tests in Tension on Combined Joints (Kommerell & Bierett,[213] 1934)

Specimen Series	Area of Weld-in	Static Tensile Strength, psi	Tensile Fatigue Strength 2×10^6 Cycles; psi S_o	$S_o - S_u$	Fatigue Fracture
1	Only Riveted	34,400	23,200	16,200	In Rivets
2	Only Welded but with Rivet Holes	56,600	24,200	17,200	Started at Inner End of Welded Seam
3*a*	2.46	61,000	30,600	23,600	Started at Inner End of Seam in Cover Plates, Then Spread to Rivet Holes
3*b* (Same as 3*a*)	2.46	Not tested	30,200	23,200	Same as 3(*a*)
3*c*	2.47	66,300	26,600	19,600	Started at Inner End of Seam in Cover Plates, Then Spread through Plate
3*d*	3.47	70,500	26,400	19,400	Same as 3(*c*)
3*e*	2.47	Not tested	32,200	25,200	In Plate at Junction of Plate with Normal Shear Weld
4	Only Riveted	38,400	21,000	15,000	In Rivets
6	3.92	38,100	22,500	16,400	Same as 3(*a*)
7	3.92	56,500	22,500	16,800	In Cover Plates through Innermost Rivet Hole

S_o = Upper Stress in Pulsating Tension Fatigue.
S_u = Lower Stress in Pulsating Tension Fatigue.

The results show that series 3(*c*) and 3(*d*) are not good in fatigue, the fatigue stress being only 19,000 psi based on the actual area of fracture. Series 3(*e*) is better than 3(*a*) because the fatigue value of the cover plates is fully developed as in series 7. The low fatigue value of 7 as compared with 3(*e*) is explained probably by stress distribution and larger dimensions. The dynamic preloading of series 3(*b*) did not noticeably affect the fatigue strength. The larger seam cross section of 3(*d*) as compared with 3(*c*) is not effective in fatigue. The welding in combined joints should be so located that the beginning of the seam lies in the cross sections in which a greater proportion of the load is already transferred through the rivets to the plates. In the larger types of riveted joints it is not sufficient to take into account only the innermost series of rivets. In this way the notch effect due to the end of a seam is transferred to a less highly-stressed location, and the innermost rivets are relieved because the bellying compressive force on these rivets due to contraction of the rivet holes is decreased.

Kommerell and Bierett conclude, in their later report, that the effect of strengthening by welding is not so great in fatigue as in static load conditions. Series 3(*c*) and (*d*) are better in static load than series 3(*a*) and (*e*); in fatigue the position is reversed. The clamping force of a riveted joint is actually raised by the increased loads placed on the joint after strengthening by welding. Donkin[214] states that welding intended to strengthen riveted joints must be designed to take the whole load in order that plastic yielding will not take place in the neighborhood of the weld and lead to fatigue failure. A similar recommendation is made in the Bridge Specifications of the AMERICAN WELDING SOCIETY. (See Bernhard's eccentric-pulsator tests on strengthened bridges; section on *Bridges*.)

Comparison of Welding with Riveting

The fatigue strength of riveted joints has been investigated principally by Schaechterle[215] and Graf;[216] a few results on riveted joints have been included in the preceding section.

The fatigue strength of welds has unquestionably been more arduously investigated than the fatigue of rivets but it is the general concensus of opinion based on results that the stress distribution in riveted joints is no less complex than in welds and that there is no great difference in fatigue value between the two methods of joining. For example, one of the conclusions reached at the 1936 Congress of the International Association of Bridge and Structural Engineering[282] was that butt welds are at least as good in fatigue as ordinary riveted joints. The comparison between welded and riveted connections is also made in the section on tests of welded structures (Bissell, Spindler, Bernhard and others). Graf,[216] Aysslinger,[115] and others have shown that the decrease in pulsating tension fatigue limit of mild steel due to a central, transverse hole is about 20%.

The results of Graf[216] and Memmler, Bierett and Gehler[9] place the pulsating tension fatigue strength of double-riveted joints in mild steel at 25,600 to 28,400 psi calculated on the weakened cross section, or 21,400 to 22,800 psi on the original unpunched plate, assuming a

hole cross section of 20%. In 1919, Abell[40] found that in reversed bending as a simple beam, butt welds (60° V., flux-coated electrodes, no reverse run) had 70% of the fatigue value of unwelded; lap welds and riveted joints had only 60%. Lea[46] states that riveted and welded joints are equivalent in fatigue (no details given) but Roš[216] found gas welds superior in pulsating tension to riveted joints of about the same static strength. In Hertel's fatigue tests of spars (980 cpm.) fabricated from corrugated Cr-Ni sheet (178,000 psi tensile strength) the spot-welded spar had a pulsating bending (0 to max.) fatigue strength of 27,000 psi, the riveted 21,900 psi. The ratio of pulsating bending (0 to max.) fatigue strength to static tensile strength was:

Pulsating Bend Tests of Aircraft Spars. Hertel[217] (1931)

Type of Spar	Ratio: Pulsating Tension Fatigue Strength / Static Tensile Strength
Box Spar; Spot-Welded Cr-Ni	0.15
Box Spar; Riveted Cr-Ni	0.125
Steel Framework Spar, Riveted	0.032–0.061
Dural Framework Spar, Riveted	0.08
Spruce Spar; Glued Joints	0.45 (Based on Compressive Strength)

Fatigue failure of the riveted spars started at the rivet holes. The high ratio for glued spruce spars shows that gluing creates much less stress concentration than welding or riveting. Schlyter[218] found that the rotating bend fatigue strength of cold-glued Sitka spruce with the joint at right angles to the plane of deformation and at an inclination of 1:12 to the grain was the same as solid, unglued specimens. Aysslinger[115] states that the fatigue efficiency of double-riveted joints is 0.57 (static efficiency 0.70). Vibration tests by Dobson and Taylor[280] on seam-welded "Tutor" steel spars and riveted "Tutor" spars yielded the following results. The failure of the welded spar occurred through the web at a rivet hole used for the attachment of the loading box. An anonymous writer[256] tested a spot-welded 18–8 monoplane wing rib at half its design load, the spar points being vibrated 1150 times per minute, $^1/_{32}$ inch amplitude, for 50 hours without failure. Subsequently the rib withstood a static load test at full design load.

Pulsating Bend Tests of Aircraft Spars. Dobson and Taylor[280] (1936)

Spar	Amplitude, Inch	Stress, psi: Calculated	Stress, psi: Actual	Number of Reversals	Results
Riveted	0.066 to 0.132	18,000 to 36,000	.....	3,764,800	Satisfactory
Welded	0.066 to 0.132	18,000 to 36,000	.....	3,764,800	Satisfactory
Riveted	0.066 to 0.186	18,000 to 50,500	.....	3,764,800	Satisfactory
Welded	0.066 to 0.186	18,000 to 50,500	.....	3,764,800	Satisfactory
Riveted	0.066 to 0.316	18,000 to 86,000	16,700 to 75,500	3,764,800	Satisfactory
Welded	0.066 to 0.316	18,000 to 86,000	16,600 to 76,000	3,764,800	Satisfactory
Riveted	0.066 to 0.353	18,000 to 96,000	16,700 to 83,000	96,800 104,200	Web Failed Bottom Boom Failed
Welded	0.066 to 0.353	18,000 to 96,000	16,600 to 86,000	141,550	Bottom Boom and Bottom of Web Failed

According to Graf,[219] the fatigue strengths of welded and riveted joints do not differ greatly; the slightly higher permissible stresses for riveted construction in high strength steel is a result of the higher standard of workmanship that can be expected in riveted joint. Schuster[88] believes that perfectly sound weld metal in boiler welds would give better service when subjected to fluctuating internal pressure than the solid plate of a riveted boiler with thicker plates. Herold[220] considers that riveted joints have the advantage that they can "breathe," but, as Tracy[221] points out, this is not always an advantage. Hankins and Thorpe[20] state that there are indications that fatigue may be more important in welded than in riveted structures. Townshend[222] states that unmachined double-V butt welds made with a high quality electrode have higher reversed-bend fatigue strength than riveted overlapped joints. The comparative fatigue tests on welded and riveted joints carried out by Lloyds in 1918, according to Thomson,[223] showed the superiority of welding. On the basis of pulsator tests on welded beams and riveted beams Witt[224] concludes that the welded beam is 50% superior in fatigue to riveted beams of the same static resisting moment.

Bridges

Experimental fatigue tests of welded bridges (as distinguished from traffic tests which have been satisfactory, or from fatigue tests of individual members) have been made by the German Railways (Bernhard[225] and Schaper[226]) and by Patton and co-workers.[227] These tests are fundamentally different from strain and vibration measurements provided by the Telemeter, Stereo-Comparator, and other devices (see Kulka,[228] Report of Bridge Stress Committee, London, 1928, and others). A pulsator consisting of two rotating eccentrics (see Appendix A where Gehler's system of fatigue testing is described, and the monograph of Späth[229]) puts the bridge in forced vibration. The maximum load developed in the bridge members is calculated from measurements of deflection. The eccentric pulsator may thus be used to apply cyclic stress to an entire bridge, as in the customary fatigue test. In addition, when the pulsating load acts with a frequency equal to one of the natural frequencies of the bridge, there is resonance and the power consumption of the pulsator motor is maximum. A decrease in the natural frequency during a fatigue test is an indication of loss of rigidity under load.

The first welded bridge to be tested by Bernhard in 1929 (welding details not given) had a span of 30 feet and failed after only 22,400 cycles (40 minutes) at max. tensile stress 22,700 psi, max. compressive stress—18,800 psi. The stresses were only +8850, −4950 psi, just before rupture. The natural frequency rose slightly in the early stages of the test indicating that the bridge actually became stiffer after repeated loading. Bernhard considered the results as unsatisfactory, but later tests on welded experimental bridges tested by eccentric pulsators at over one-half the maximum design stress were entirely satisfactory. Bernhard also showed that riveted bridges strengthened by welding were stiffened, the natural frequency being increased 3 to 7% in the loaded and unloaded states. Welding decreased the damping factor, that is, the range of frequencies at resonance, and decreased stresses and deflections due to traffic. Sahling[264] found that in a wrought iron bridge whose bending deformations had been decreased 6% by strengthening with mild steel welding, the lateral vibration of the superstructure was decreased 45%. Bohny[208] also observed definite stiffening during pulsator tests of a welded bridge.

A comparison of the fatigue behavior of a welded and a riveted bridge of the same dimensions (40-ft. span) was made by Patton, Bouchtedt and Tchnoudnowsky,[227] using an eccentric pulsator. The welded bridge failed

after 215,000 cycles at a maximum load of 25 tons; the riveted failed after 250,000 cycles at the same load. The natural frequency of the welded bridge remained constant throughout the test except just before failure whereas the frequency of the riveted bridge, which was lower than that of the welded bridge at the start of the test, appeared to vary 10%. These variations were related to the warming of the riveted joints which occurred after only 30,000 cycles. The welded joints remained cool until just before failure, which occurred in the heat affected zone. Patton considered that the tests were not unfavorable to the welded bridge.

The successful traffic test of the first experimental all-welded Swiss railway bridge is described by Beguin[230] and others. The test bridge consisted of two panels of stringers, a joint of a truss, and a cross girder. The stringers were butt welded; the girders were butt-fillet welded together with flat webs and flanges. The bridge was placed in track with a specially wide rail joint over it, and withstood 1.5×10^6 cycles of load in five years, the welds showing no signs of failure.

Vibrations

Although a few aspects of the damping and vibration characteristics of welds and welded structures have been discussed in the sections on Bridges and Repeated Impact, several investigators have studied vibrations in welds apart from the fatigue aspect. H. C. Forbes, quoted by W. L. Warner[265] made the keen observation that a vibration will be damped by a poor weld, which was later confirmed by damping tests of good welds made by Hallström.[252] In these tests of cantilever bars (0.31 in. $\times$ 0.79 in.) of mild steel with a vibrating length and amplitude of 38 in. and 1.18 in., respectively, the unwelded bar came to rest after 120 sec., the arc-welded bar after 45 sec.

The advantages of welding in preventing vibration in machinery, as pointed out by Chapman,[266] are connected with the higher modulus of elasticity of welded steel as compared with cast iron. The closed section, ideal for preventing vibrations, is easy to weld but difficult to cast. Krug[267] and Tweetside[268] also found that welded construction for machine tools fulfills the two requirements of lightness and rigidity essential for combating vibrations better than cast-iron construction. Föppl,[272] however, believes that welds do not develop any more damping than the base material and that, when riveting is replaced by welding, the welded steel should have high damping capacity.

The vibration experiments on welded lathe beds made by the Technical College, Berlin-Charlottenburg, jointly with the Siemens-Schuckert concern[269] confirm the experience of Chapman and others. These laboratories also made vibration tests on solid cast iron and steel I-beams as well as on welded I-beams. The results are shown in the preceding table. The welded beam (*c*) was made of flat plates; the welded beam (*d*) was made of ribbed flanges having a "nose" profile (welding details not given). The beams were placed on knife edges 10 ft. apart and set to vibrate by means of a solenoid, 30 to 500 cycles/sec. The tests show that the welded beam (*d*) is most suitable, the cast-iron beam least suitable from the vibration standpoint.

Vibration Tests of I-Beams. Hoch and Ferney[269] (1935)

	Unwelded		Welded	
	Cast Iron	Rolled Steel	(*c*)	(*d*)
Time to Come to Rest, Free Vibrations, Sec.	0.50	0.36	0.32	0.22
Resonance Amplitude, Forced Vibrations	0.039 mm.	0.0265 mm.	0.026 mm.	0.0188 mm.
Natural Frequency, Free Vibrations	76 cycles/sec.	88.5	89.3	83.5
Natural Frequency, Forced Vibrations	77.8 cycles/sec.	86	89.5	84
Disturbed Length, %	31	22	29	21

D. M. Warner[233] has described a machine to test the relative vibration fatigue characteristics of seam-welded joints, spot-welded baffles and other products. No results are reported but it is stated that the test reveals grain growth in the junction zone of carelessly made gas welds. In ship construction, according to Moore,[202] a bracket welded at the point of greatest amplitude will usually damp out the vibrations. Helsby, Hamann and Samuely[270] state that the inequality of stress in a weld in longitudinal shear is a great advantage in damping vibration.

The specification of the Department of Commerce in 1931 that spot-welded ribs of airplane wings pass a 10-hour vibration test was successfully met by the manufacturers concerned. Hoglund[288] mentions a 25-hour vibration test which seam- and spot-welded aluminum gasoline tanks must withstand. A device for preventing fatigue in welded aluminum tanks has been patented.[289]

Tubes

The fatigue value of welds in aircraft structural tubing has been investigated mainly by rotating bend tests on individual gas butt welds. There are large differences in fatigue strengths reported by the investigators for apparently similar material. For example, Miss Dousin,[231] Hoffmann,[77,112] Johnson[66] and Beissner[103] agree that gas-welded plain carbon and Cr-Mo tubing has a fatigue value of 14,000 to 16,000 psi. Baumgärtel,[111] however, found about 20,000 psi for Cr-Mo tubes using low-carbon or Cr-Mo fillers, Matthaes[232] 20,000 to 25,000 psi for plain carbon and 28,000 for Cr-Mo (no details), Sutton,[233] 20,200 for heat treated Cr-Mo (iron or Cr-Mo filler), and Wegelius[129] 25,000–28,500 for plain carbon and 30,800 for Cr-Mo (filler not mentioned), all using unmachined specimens. The lowest of these values is higher than any given by R. R. Moore[1] in 1927. The effect of differences in welding technique, such as heat effect, grain size, and penetration, must therefore be considerable; for differences of any magnitude have not been found for variations in chemical composition, machining or internal stresses. Flash welds in Cr-Mo tubing gave 32,000 after stress annealing, according to Johnson,[66] but gave low values (13,000) in plain carbon. Oxyacetylene welds in 18–8 tubing (1 inch o. d., $^1/_{16}$ inch wall) gave 16,000 psi as-welded or after 30 minutes annealing at 2000° F. (1100° C.), but 23,000 psi after 30 minutes at 1600° F. (875° C.). The British Aeronautical Research Committee[291] found no difference in fatigue strength of Cr-Mo and C-Mn welded tubes (no details).

Reversed bend tests represent a less severe test than rotating bend. Müller[128] found, using the reversed bend method, that 0.11% C tubing gave 25,000 psi, 0.32% C, 29,000, and Cr-Mo 24,000 to 31,000 depending on heat treatment. For low carbon superheater tubing Ulrich[234] found about 15,000 for gas welds but less than 10,000 for arc welds. Internal water pressure (500 psi) with accompanying corrosive effect applied to the tubes during test had no appreciable effect on fatigue value. In pulsating tension Ulrich found 13,000 for gas welds

in superheater tubing, and Matthaes[232] gives 40,000 for Cr-Mo tubing.

Recently, fatigue tests have been made in which service conditions are more nearly duplicated than in the rotating bend test. Ward,[102] using the stationary cantilever type machine, found 25,000 psi for as-welded Cr-Mo tubing and 35,000 for heat treated; these values are, respectively, 1/4 and 1/3 the static tensile strength of the as-welded tube. Similar ratios for the rotating bend test vary from 20 to 50%. The ratio of fatigue strength welded to that unwelded is in the neighborhood of 60% for all types of tests. In alloy tubing (Cr-Mo and Cr-V), Füchsel[235] states that the ratio of welded to unwelded torsion fatigue strength is 65%. In general, as the carbon (0.25–0.40% C) or alloy content (Cr, Mo or Mn) of the tube is raised the ratio of endurance limit to static tensile strength of the weld is lowered from 50 to 20%. This is shown by almost all investigators.

The other type of fatigue test for welded tubing that simulates service conditions is the vibration test. This test as described by Warner[236] is only comparative. The welded tube or structure is submitted to simple translational vibrations of known amplitude. A vibration test of welded tubes carried out by an English tube works and quoted by Roosenschoon[116] showed that such variables as structural welding stresses, as distinct from local shrinkage stresses, have a considerable effect on fatigue resistance, especially with Cr-Mo tubing.

The effect of chemical composition, whether of tubing or filler rod is not great. The difference in fatigue value of gas welds in plain carbon tubes of 0.1 and 0.3% C is not over 15%. Nor is the difference between plain-carbon and Cr-Mo tubing of similar carbon content over 10 to 15% and the scatter seems to be the same for both. The important consideration is evidently that the tubing be as insensitive as possible to the welding heat and this seems to be attained by using a low (0.25–0.30% C) rather than a high carbon content (0.30–0.35% C) in Cr-Mo steel. Apart from differences in weldability, the composition of the filler rod was not important in these tests. Beissner[103] found that an alloy (Cr-Mo) filler rod was better for thick tubes (0.12 in.) but that plain carbon was superior for thin tubes (0.06 in.). He also found that there is an optimum breadth of reinforcement for each size of tube. The medium-manganese (0.3 C, 1.5 Mn) tube has been found inferior in fatigue. Air hardening has practically no effect on fatigue cracking in Cr-Mo welds.

Lap and fish-mouth joints appear to be at least as good as butt joints, according to Miss Doussin and Johnson, but brazed, soldered and bell-and-socket joints are definitely inferior, as Miss Doussin and Hoffmann have shown. Pinned and riveted joints have only 50 to 80% of the strength of welds. If the factor of ease of welding is not considered there is no size effect, at least up to tubes 3 in. in diameter.

The three most important causes for fatigue failure in welded tubes are:

(1) Poor penetration and inclusions; a service fatigue failure in superheater tubing owing to poor penetration has been described by Pfleiderer.[70]

(2) Heat effect; if poor penetration has been overcome, the micro-structural changes due to torch heat, which extends for several inches on either side of the weld in tubing, is of prime importance, as Ward's and Beissner's micrographs show. A coarse Widmannstätten structure is stated to be particularly dangerous.

(3) Shrinkage stresses; although especially important in completed structures, Johnson showed that a stress anneal at 950° F. increased the endurance limit of flash welded Cr-Mo joints by 30%.

It is surprising that the removal of reinforcement has not had beneficial effects on the fatigue resistance of tube welds. Moore found no difference between machined and unmachined welds, and Johnson found a slight effect (5% increase in fatigue value) only in tubes below 3/4 in. diameter. However, Hoffmann and Ward both recommend that reinforcement be low, and the welds concave and smooth in important joints. If distortion and decarburization can be prevented, heat treatment improves the fatigue strength of welds in Cr-Mo tubing up to 40%.

APPENDIX A

Methods of Testing Welds in Fatigue

Most of the investigators have used the rotating-bend type with two-point (Farmer type) or one-point (Föppl) loading. The design of welded specimen for this type of

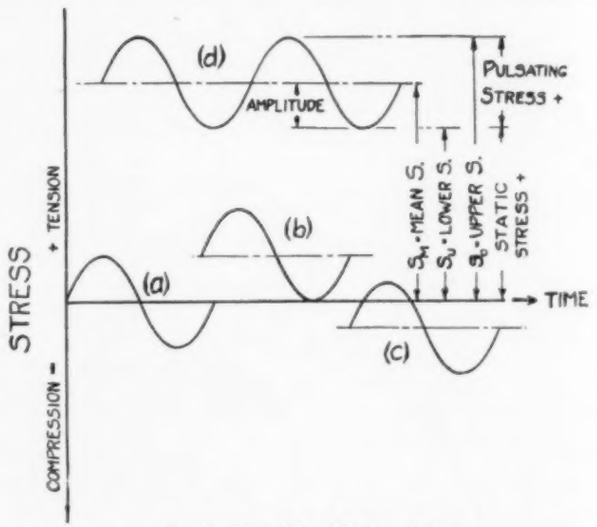


Fig. 19—Types of Load Cycles in Fatigue

machine has been discussed by Thornton,[92] Zimmerman,[237] and Weinman[19] showed that the deflection of a welded specimen in a Farmer machine remains constant throughout the duration of the test until very shortly before fracture, if the stress exceeds the endurance limit.

The direct stress type has also been popular. Machines of this type apply direct tension and compression; the cycle of loading may consist, for example, of: (Fig. 19) (*a*) alternating stress, $\pm S_{max.}$; (*b*) pulsating tension, $+S_{max.}/0$; (*c*) alternating with superimposed static compression $+S_{min.}/-S_{max.}$; (*d*) pulsating with superimposed static tension $+S_{max.}/+S_{min.}$. Endurance limits are quoted as $S_{0(Sm)}$; $S_m = 1/2\ S_0$ represents the pulsating tension fatigue strength; $S_m = 0$ represents the customary alternating stress endurance limit. Such load cycles are provided by machines of the magnetic (Haigh) and hydraulic types (Amsler Pulsators up to 200 tons capacity). Objection to the former type has been made by Orr,[238] who points out that eccentricity of loading is possible with welds. The accuracy of the Amsler pulsator has been investigated by Schick[10] who found 12 to 28% errors at 120 to 460 cpm. (cycles per minute). Graf[239] also found considerable differences with riveted joints between 1, 10 and 350 cpm., the result at 10 cpm. being on the safe side. Using the Schenck high-frequency tension-compression machine Laute[141] found no

appreciable reduction in fatigue value of welds at 30,000 cpm. The Wazau pulsator[94] provides a stress cycle with short rest periods at the peaks in order to eliminate the inertia effects said to exist in the usual oil-pressure pulsators. Another type of pulsating stress machine particularly adapted to the testing of large welded specimens is the vibrating bridge.[9] The specimen acts as a tension member in a framework truss of 50 ft. span loaded to any desired extent by dead weight or by smooth cycle pulsating load. The pulsating load is provided by two rotating eccentrics.

Three other types of fatigue tests that have been used for welds are reversed (back-and-forth) bend, rotating-spring cantilever, and reversed torsion. Of the numerous reversed-bend machines the following may be mentioned; the Schenck machine that can be adapted to 90° and T welds, the Föppl-Heydekampf machine modified by Friedmann[79] to test welded wire, the admirably simple machine devised by Orr,[71] and Dörnen's low-frequency tester.[90] An excellent description of the calibration of a reversed-bend machine for welds is given by Thum and Lipp.[15] The design of specimens for the cantilever machine is discussed by Jennings,[89] who, among others,[240] clarifies the question of small versus large specimens. Fatigue specimens smaller than about 1/4 in. diameter should not generally be used for welds, and, as pointed out in the section on Methods of Design fatigue limits from specimens of any size should never be used directly as design stresses. Peterson[241] found that the same endurance limit was obtained with welds 0.3 in. diameter as 1 in. diameter. Lea and Parker[21] found that fatigue limits on machined specimens of 70° V welds or all-weld-metal deposited by coated electrodes were practically identical on reversed-bend and rotating-bend machines. Results from the Haigh direct stress machine were 30 to 50% lower, however, the explanation appearing to be that the Haigh machine develops maximum stress concentration around all flaws in the cross section of the specimen whereas only the surface flaws are subjected to maximum stress in the reversed- or rotating-bend tests.

Special fatigue machines for welds or welded structures have been devised by Colinet[242] (a 4-ton repeated bend machine at 15 cpm., span 16 feet, with or without shock), Warner[236] (vibration tester for welded tanks), Wilson[76] (motor-driven eccentric for column connections) and others. A reversed-bend fatigue test is a routine test which all welders on the North-Western Railway, India,[243] must pass every month. Henderson[271] states that the welders assigned to welding railway freight and passenger cars on a British railway must prepare specimens once a month to pass an alternating stress test (no details). Bright,[244] Thom[245] and Green[246] describe shop fatigue tests for welds.

Short-cycle (short-time) methods for determining the fatigue limit of welds are generally unconvincing. The method of Stanton and Pannell,[32] whose comparison-curve method was the first to be applied to welds (1911) has recently been adopted again.[71] Bartels[38] has shown that the Lehr power-output method, and the deflection and rise-in-temperature methods, to a limited extent, give rather close approximations of the endurance limit of welds.

A method of presenting fatigue results on welds has recently been proposed by Dutilleul,[2] who observed in rotating cantilever tests that the different degrees of porosity in different specimens cut from the same welded joint gave rise to different fatigue values. Thus, of three specimens cut from the same weld and tested at 21,000 psi, two fractured before 10×10^6 cycles. Nevertheless, three other specimens from the same weld and tested at 24,200 psi withstood 10×10^6 cycles without fracture. Dutilleul, therefore, defines an endurance coefficient C showing the scatter in results.

$$C = \frac{n_1F_1 + n_2F_2 + \dots}{N_1F_1 + N_2F_2 + \dots}$$

where $F_1F_2 \dots$ are the fatigue stresses, n_1 is the number of specimens not broken at F_1, etc., and N_1 is the total number tested at F_1, etc. He states that the coefficient is important only when F_1N_1, F_2N_2, etc., are close together, and shows that the coefficient is reasonably reproducible for a given electrode.

APPENDIX B

Tables of Results of Fatigue Tests

NOTE: The details of materials and testing contained in the tables are as complete as possible. Where type of electrode, weld or welding process is not given, it indicates that the original article does not contain the information. Numerals in parentheses refer to footnotes.

Results of Tests

Butt Welds

The numerical results of fatigue tests on butt welds in mild steel are collected in Tables 1 to 4. These are self-explanatory, available details of test conditions being given as concisely as possible. It would be presumptuous to compute an average value of endurance ratio or fatigue strength from these tables (see section on Methods of Design). Yet it appears that there is remarkably little difference in the ranges of fatigue strength reported for direct-stress, and reversed-, and rotating-bending. There have been numerous estimates of the necessary cycles criterion for welds in fatigue, Haigh[68] giving 2×10^6 cycles for dense welds and 5×10^6 cycles for defective welds in direct stress. It seems quite certain the Wöhler curves for welds in steel do not generally become horizontal in any type of stress at 2×10^6 cycles. Thum and Lipp[15] have found that 100×10^6 cycles is an absolute criterion for welds in reversed bending. Matting and Otte[274] gripped their pulsating tension fatigue specimens close to the weld so that fracture was forced to occur in weld or junction zone. The numerous factors that affect fatigue value, noted in the tables, are discussed in detail in other sections of the review. It is unfortunate that standard welding rods are not available so that the results of these elaborate fatigue investigations will not become obsolete with the wires themselves.

Fillet Welds

Selected results of fatigue tests on fillet welds are given in Table 5. All investigators used mild steel; none of the specimens was machined. The tests of Memmler, Bierett and Gehler[9] (since endurance limits were not determined, these tests are not reproduced), were performed on two pulsating bridges (240 cpm.), and a Losenhausen pulsator (360 to 660 cpm.). The arc welds were made with bare electrodes. Throat dimensions were: Series II and IV = 0.280 in.; series V and XIII = 0.224 in. Stresses are computed on the plate cross section. Graf[6] used Amsler and Losenhausen pulsators. He quotes the original fatigue strength as the stress at which the Wöhler curve intersects the 2×10^6 abscissa. His tests were generally made with bare electrodes, which he found equivalent in fatigue value to coated electrodes, in agreement with Schick, Bierett and Roš and Eichinger (probably due to some extent to notch effect).

Roš and Eichinger[5,7] used a 50-ton Amsler pulsator at 300 cpm. Their criterion is 10^6 cycles. Their fatigue stresses are computed in the following way:

Table 1—Direct Stress Fatigue Results on Unmachined Specimens (unless otherwise noted)
(Fatigue Limit Stress Range 0 to Max. Tension Unless Otherwise Noted)

Reference and Date	Plate Material and Welding Process	Type of Weld	Fatigue Limit, psi Plate Sf. unw.	Weld Sf. w	Endurance Ratio Sf. w/Sf. unw
Scott[247]—1934	Mild Steel—Bare	60° X	±34,700	±17,700	0.51
Scott[247]—1934	Mild Steel—High Class Covered	60° X	±34,700	±20,200	0.58
Küchler[248]—1934	Ship Steel—Bare	V		15,600	
Küchler[248]—1934	Ship Steel—Heavy Coated	V		19,600	
Küchler[248]—1934	Ship Steel—High Quality Coated	V		25,600–29,800	
Küchler[248]—1934	Low-Alloy Structural Steel, Heavy Coated	V		19,500	
Küchler[248]—1934	Low-Alloy Structural Steel, High Quality Cored	V		27,700	
Aysslinger[118]—1932	Coated	V	29,900	24,200	0.81
Hankins & Thorpe[20]—1934	Structural Steel—Coated	X Unmachined	34,700	17,500	0.51(1)
Hankins & Thorpe[20]—1934	Structural Steel—Coated	X Machined	34,700	30,200	0.87(2)
Wallmann[21]—1934	Mild Steel—Carbon Arc Shielded	Small Blow-Holes	35,500	21,400	0.60
Wallmann[21]—1934	Mild Steel—Carbon Arc Shielded	Large Blow-Holes	35,500	19,900	0.56
Wallmann[21]—1934	Mild Steel—Carbon Arc Shielded	Poor Penetration	35,500	14,200	0.40
Fry[63]—1934	Mild Steel—0.1 C Bare	V	25,600±11,400	25,600±6,400	0.56
Roš & Eichinger[7]—1935	Mild Steel—Gas or Arc	V or X		17,100 to 19,900	
Schulz and Buchholtz[18]—1932	Mild Steel—Arc Bare or Coated	X	27,000	17,100	0.63
Haigh[68]—1935	Mild Steel—Arc (Many Cavities)	V		21,300	
Hankins[48]—1935	Structural Steel—Bare	X Unmachined		<13,400	
Hankins[48]—1935	Structural Steel—Bare	V Unmachined		11,200	
Hankins[48]—1935	Structural Steel—Bare	X Machined		10,100	
Hankins[48]—1935	Structural Steel—Bare	V Machined		9,000	
Graf[6]—1931–1935	Mild Steel—Bare and Coated Arc, Gas	Root Welded		25,600 Unmach.	0.64
		V 60° and 70°	39,800	34,100 Mach.	0.86
Thum & Schick[249]—1933	Mild Steel—Bare	X		25,600±5,700	
		V		25,600±6,400	
Matting & Otte[274]—1936	Mild Steel. Special Gas	X (530 cpm.,	37,000	27,500	0.74
Matting & Otte[274]—1936	Unpeened Ordinary Gas	X 2×10^6	37,000	22,200	0.60
Matting & Otte[274]—1936	Normalized Ordinary Gas	X cycles)	37,000	25,000	0.68
Matting & Otte[274]—1936 (See Section on Notched-Bar Fatigue Tests)	Coated Electrode	X	37,000	24,000	0.65

Table 2—Reversed Bend Fatigue Results on Machined Specimens (unless otherwise noted)
(Fatigue Limit Stress Range ± Max. Unless Otherwise Noted)

Reference and Date	Plate Material	Welding Process or Rod	Type of Weld	Fatigue Limit, psi Plate Sf. unw.	Weld Sf. w	Endurance Ratio Sf. w/Sf. unw
Joellenbeck and Massmann[160]—1931	Boiler Plate About 0.05 C	Arc	..	28,500	20,600–22,800	0.7–0.8
Jünger[42]—1931	Boiler Plate		V	21,300 Mill Scale	19,900	0.94
Jünger[42]—1931	Boiler Plate		V	26,700 Mill Scale	24,900	0.93
Lohmann and Schulz[34]—1934	Mild Steel	Bare	X Unmach.	32,700	18,500	0.57
Lohmann and Schulz[34]—1934	Mild Steel	Coated	X Unmach.	32,700	17,000 Unannealed	0.52
					27,000 Annealed	0.83
Lohmann and Schulz[34]—1934	Low-Alloy Structural St.	Bare	X Unmach.	39,800	22,800	0.57
Lohmann and Schulz[34]—1934	Low-Alloy Structural St.	Coated	X Unmach.	39,800	21,400 Unannealed	0.54
Orr[71]—1935	Mild Steel	Arc	U	26,900	21,300	0.79(3)
Thierens[131]—1935	Mild Steel	Arc	V	25,300	25,400	1.0
Thierens[131]—1935	Low-Alloy Steel	Arc	V	38,600	27,400	0.71
Roš & Eichinger[7]—1935	Mild Steel	Gas or Arc	V		28,500 0 to Max.	
					±19,900	
Lea and Parker[21]—1936	Mild Steel	Coated	V Unmach.		±19,700	
Lea and Parker[21]—1936	Mild Steel	Coated	V Mach.		±24,000	
Kirkcaldy[250]—1929	Mild Steel		Mach.		19,500	

Normal-shear fillet weld = Load/$2b\,h$

Parallel-shear fillet weld = Load/$2b\,s$

where b = breadth of cover plate (not including weld); s and h = height of deposit with respect to cover plate. Bare and coated electrodes were used.

Hankins and Thorpe[20] tested their specimens in a 6-ton electromagnetic (Haigh) machine at 2300 cpm. Class *A* structural steel and high-class covered electrodes were used. The fatigue stress is computed on the throat area. The criterion of fatigue limit was 25×10^6 cycles. The fatigue strengths were only about $^1/_4$ the static strength of the specimens, which is in general agreement with Graf. Schick[10] used bare electrodes (0.08 C, 0.5 Mn, 0.01 Si; $^3/_{16}$ in. diam.), and tested his specimens in a specially-calibrated Amsler pulsator.

According to the latest German results (Graf and Kommerell[143]), fillet welds are equivalent in fatigue strength to butt welds (no details are given). It should be noted that Schick's specimen 26 was equivalent in fatigue value to butt welds at 2×10^6 cycles.

Schulz and Buchholtz,[18] and Fry[63] also report a few results of pulsator tests with fillet welds. Butt fillet welds were tested by Jennings[89] in the cantilever machine. Nikolaev[261] tested butt and fillet welds in mild steel in alternating tension and compression, but his tests extended only to about 100,000 cycles. The specimens

Table 3—Rotating Bend Fatigue Results on Machined Specimens (unless otherwise noted)

Reference and Date	Plate Material	Welding Process or Rod	Type of Weld	Fatigue Limit, psi Plate Sf. unw.	Fatigue Limit, psi Weld Sf. w.	Endurance Ratio Sf.w/Sf. unw.
Pester & Schulz[41]—1932	Mild Steel	Gas	X	24,600	18,500	0.75
Pester & Schulz[41]—1932	Mild Steel	Gas Peened	X	24,600	23,600	0.96
Greger[251]—1933	Mild Steel	Low C Gas	V	33,400	26,300	0.79(4)
Musatti & Reggiori[60]—1934	Mild Steel 0.2 C	Coated	X	41,000	27,600	0.67(5)
Musatti & Reggiori[60]—1934	Mild Steel	Coated Low C	All-Weld-Metal	41,000	29,000	0.71(6)
Hankins & Thorpe[20]—1934	Str. Steel	Coated	All-Weld-Metal	26,800	18,400	0.69
Abell[40]—1919	Mild Steel	Fluxed	X	23,500	14,600	0.62
Blackwood[62]—1933	Mild Steel	Coated	90° V		19,000	(7)
Blackwood[62]—1933	Mild Steel	Bare	90° V		20,000	(8)
Bartels[28]—1930	Mild Steel	0.04 C Gas	V	39,800	24,100	0.86(9)
Thierens[121]—1935	Mild Steel	Arc	V	28,600	28,600	1.0
Thierens[121]—1935	Low-Alloy Str. Steel	Arc	V	46,000	38,000	0.83
Gerritsen[75]—1933	Mild and Low-Alloy St.	Arc	V		27,200–30,600	
Roš & Eichinger[7]—1935	Mild Steel	Gas or Arc	V		18,500–19,900	
Hallström[252]—1931	Mild Steel 0.20 C	Arc	X	31,300	25,600	0.82
Hallström[252]—1931	Mild Steel 0.20 C	Gas	X	31,300	24,200	0.77
Brown[124]—1935					18,000	(10)
Hodge[61]—1935					16,000–18,000 Porous 30,000 Sound	
Lincoln[253]—1935					10,000–12,000 Bare 30,000 Shielded	
Roš & Eichinger[254]—1935	Basic Bessemer	Arc			16,600–18,400	
Roš & Eichinger[254]—1935	Open Hearth	Arc			18,000–21,600	
Sulzer[63]—1933	Boiler Plate	Arc	All-Weld-Metal		22,400 Annealed 14,600 Unannealed	
Dutilleul[2]—1936	No Details	Arc	V, 7 Passes 3/16 In. Electrodes	31,300–33,400	About 18,500	(11)
Kjellberg[255]—1920	Mild Steel 11/16 In. Plate	Arc	Coated Electrodes		25,600	
Lincoln Elec. Co.[256]—1935	Mild Steel	Bare or Washed Electrodes	All-Weld-Metal	28,000	12,000–15,000	0.50
Lincoln Elec Co.[214]—1935	Mild Steel	Shielded Arc	All-Weld-Metal	28,000	28,000–30,000	1.0
Yarko & Abramova[257]—1935	Mild Steel 0.21 C	Chalk Coated	V	...	20,000	
Yarko & Abramova[257]—1935	Mild Steel 0.21 C	Other	V	...	12,000–22,800	0.28–0.50
Yarko & Abramova[257]—1935	Mild Steel 0.21 C	Bare Coated	V	...	18,500	
Yarko & Abramova[257]—1935	Mild Steel 0.21 C	Gas	V	...	18,500	
Jennings[134]—1933	Hot-Rolled Steel	Bare	V	27,000	16,000	0.59
Jennings[134]—1933	Hot-Rolled Steel	Fluxed	V	27,000	25,000	0.93
Peterson & Jennings[72]—1931	Mild Steel	Bare	Mach. All-Weld-Metal		9–13,000	(12)
Peterson & Jennings[72]—1931	Mild Steel	Bare	Unmach. All-Weld-Metal		12–16,000	(12)
Peterson & Jennings[72]—1931	Mild Steel	Fluxed	Unmach. All-Weld-Metal		31,000	(12)
Jennings[95]—1934	Mild Steel	Bare	30° V		19,180	
Zimmerman[237]—1934	Mild Steel	Heavy Coated	V	34,000	20,800	0.61(13)
Zimmerman[237]—1934	Mild Steel	Gas	V	34,000	19,700	0.58(14)
Thornton[92]—1934	15–20 Boiler Plate	Gas	V Low C Rod	32,500	14,900	0.46
Thornton[92]—1934	15–20 Boiler Plate	Gas	X Low C Rod	32,500	10,000	0.31
Thornton[92]—1934	15–20 Boiler Plate	Low Carbon—Bare	V	32,500	20,000	0.62
Thornton[92]—1934	15–20 Boiler Plate	Low Carbon—Bare	X	32,500	13,000	0.40
Thornton[92]—1934	15–20 Boiler Plate	Covered	V	32,500	27,000	0.83
Jennings[89]—1930 (also Lobo—1929)	Hot-Rolled Steel	Bare Low C	0°	27,000	16,000	0.59
Jennings[89]—1930 (also Lobo—1929)	Hot-Rolled Steel	Bare Low C	30° V	27,000	21,000	0.78
Jennings[89]—1930 (also Lobo—1929)	Hot-Rolled Steel	Bare Low C	45° V	27,000	20,100	0.74
Jennings[89]—1930 (also Lobo—1929)	Hot-Rolled Steel	Bare Low C	30° X	27,000	16,200	0.60
Jennings[89]—1930 (also Lobo—1929)	Hot-Rolled Steel	Bare Low C	45° X	27,000	17,800	0.66
Moore & Kommers[258]—1927						(15)
Muller[259]—1936		Bare or Dipped			16,000–18,000	
Muller[259]—1936		Heavy Coated			26,000–32,000	
Lohmann & Schulz[24]—1934	Mild and Low-Alloy Structural Steel	Bare	X		17,000–18,000	
Lohmann & Schulz[24]—1934	Mild and Low-Alloy Structural Steel	Covered	X		25,600–27,000	
Smith[293]—1936	All-Weld Metal	Bare or Lightly Coated			12,000–15,000	
Smith[293]—1936	All-Weld Metal	Shielded Arc			30,000	
Michel[294]—1932	Mild Steel (64,000 psi Tensile)	Arc			28,500	

Table 4—Torsion Fatigue Results on Machined Specimens (Fatigue Limit Stress Range ± Max. Unless Otherwise Noted)

Reference and Date	Plate Material	Welding Process or Rod	Type of Weld	Fatigue Limit, psi Plate Sf. unw.	Fatigue Limit, psi Weld Sf. w.	Endurance Ratio Sf. w/Sf. unw.
Garre & Gerbes[260]—1931	Mild Steel	Gas 0.06 C	60° X	22,400	20,400	0.91
Garre & Gerbes[260]—1931	Mild Steel	At H_2 Low C	60° X	22,400	19,200	0.86
Garre & Gerbes[260]—1931	Mild Steel	Flash	Annealed 15 Min. at 800° C.	22,400	19,500	0.87
Garre & Gerbes[260]—1931	0.35–0.40% C	Gas	60° X	37,700	19,200	0.51
Garre & Gerbes[260]—1931	0.35–0.40% C	At H_2 Low C	60° X	37,700	29,400	0.78
Garre & Gerbes[260]—1931	0.35–0.40% C	Flash	Annealed 15 Min. at 800° C.	37,700	26,600	0.71
Rosenberg[29]—1934	Mild Steel	Flash			Approx. 22,800	1.0
Thierens[121]—1935	Mild Steel	Arc	V	16,400	16,400	1.0
Thierens[121]—1935	Low-Alloy Structural St.	Arc	V	23,800	22,600	0.95
Roš & Eichinger[5,7]—1935	Mild Steel	Gas, Arc	V		25,600; 0 to Max. ±15,700	
Roessler[309]—1936 see Section on Tests of Welded Structures	Mild Steel	Arc	V (90°, Circular Bead)		18,000; 0 to Max. ±8,500	

Table 5—Results of Fatigue Test on Fillet Welds

Type of Joint	Lower Stress psi S_u	Upper Stress psi S_o	Cycles to Fracture $\times 10^6$	Fatigue Strength S_f
	O. Graf[6] 1931–1935			
Arc 27				18,500
Arc 28				14,200
Gas 31				18,500
Gas 32				24,200
Gas 33				25,600
Arc 44 (L = $1^3/_{16}$ in.)				12,800
Arc 44 (L = 8 in.)				15,700
L = Gap Between Main Plates				
Arc 40				14,200
Arc 41				12,800
Arc 42				10,000
Arc 46				10,000
Gas 46				15,700
	W. Schick,[10] 1934			
13	15,200	22,800	1.0	
14	15,200	22,800	0.7	
16	17,100	25,600	1.0	
18	17,100	25,600	0.3	
20	17,100	25,600	2.0	
21	20,400	30,600	0.5	
23	20,400	30,600	0.6	
26	21,800	32,700	2.0	
27 I	17,800	33,400	2.0	
27 IV	17,800	32,300	2.0	

acted as members in a 30-ft. bridge span equipped with an eccentric oscillator (20 cpm.). Patton, Gorbunow and Berstein[298] also tested covered electrode welds in a bridge equipped with an eccentric pulsator.

T Joints

Selected results of fatigue tests on fillet welds in mild steel are given in Table 6. All results, unless otherwise noted, are for unmachined mild steel. The welded specimens tested by Thum and Lipp[15] were prepared by a commercial firm using mild steel (0.1 C, 0.4 Mn) and bare electrodes. The cast steel contained 0.12 C, 0.31 Si, 0.88 Mn, 0.066 P, 0.041 S; the gray cast iron analyzed 3.05 C, 2.24 Si, 0.84 Mn, 0.13 P, 0.12 S; both were horizontally cast. Specially-calibrated reversed-bend machines were used (1000 cpm. for small specimens; 2000–3500 cpm. for large). The leg of the welded T was not tapered. As shown in Fig. 20 the Wöhler curve of the welded T was not horizontal at 100×10^6 cycles. Fig. 21 is a partial Goodman diagram of some of the results. The factor B_k given in the table is the notch-sensitivity fatigue factor, which includes all fatigue-lowering factors (notches, surface irregularities and shape-factor). Of all the materials tested the weld had the largest notch-sensitivity factor. The shape factor cannot be altered by using stronger steels for welding but can be favorably affected by using more ductile electrodes. The large specimens showed the same trend as the small but had lower fatigue limits. Machining the junction between surface of weld and surface of leg raised the fatigue value by 15%.

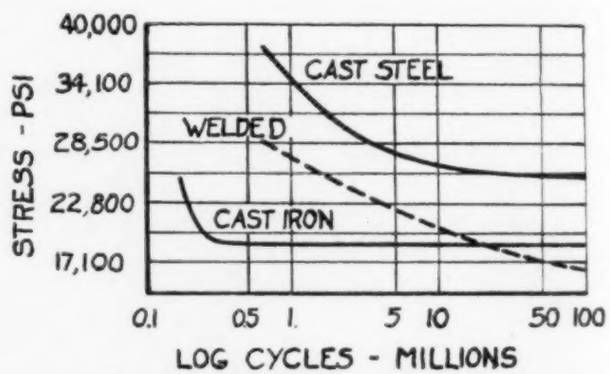


Fig. 20—Wöhler Curves in Alternating Bend for T's (Preliminary Tests, Welded T Was Made with Bare Electrode, Unmachined) Thum and Lipp[15]

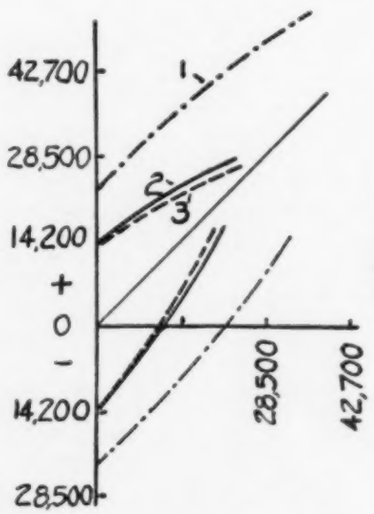


Fig. 21—Fatigue Limits in Bending of T's. 1—Cast Steel. 2—Welded Mild Steel Unmachined Convex Fillet, Bare Electrode. 3—Cast Iron, No Defects. Thum and Lipp[15]

Table 5 (Continued) Results of Fatigue Tests on Filler Welds

Type of Joint	Lower Stress, psi S_u	Upper Stress, psi S_o	Cycles to Fracture × 10^6	Fatigue Strength psi S_f
M. Roš & A. Eichinger[5,7] 1936				
Normal Shear Tension	0		>1	12,800–15,700
Normal Shear Tension	0		>1	10,000–11,200
Normal Shear Compression		0	>1	10,000
Parallel Shear Tension	0		>1	12,800–15,700
Parallel Shear Tension	0		>1	12,800
Parallel Shear Compression		0	>1	12,800
G. Bierett,[263] 1933				
A Weld Interrupted at Gap	11,000	22,000	0.46	
B Weld Continuous Across Gap	11,100	21,900	0.51	
C Machined	11,500	23,000	2.10	
A. Hankins and P. L. Thorpe,[20] 1934				
2 Normal Shear				15,700
4 Parallel Shear				13,400

Sulzer[65] tested his specimens in a special reversed-bend machine fitted with a flywheel to promote smooth operation, similar to Müller's device.[262] The specimens were arc welded (details not given), and the criterion of fatigue limit was 10×10^6 cycles. The M. A. N. magnetic reversed-bend fatigue machine was used by Jünger[43] for his arc-welded specimens (details not given). The stress was computed on the cross section of the leg. The specimens of Roš and Eichinger,[5,7] and Memmler, Bierett and Gehler[9] were tested in pulsating direct stress (see preceding section). The specimens tested by Gerritsen and Schoenmaker[132] were mild steel welded with a high-grade electrode (no details). The specimens were stressed in pulsating tension (not reversed bending); the limits of stress given in the table just failed to produce fatigue failure in 2×10^6 cycles. The fifth specimen had the same fatigue value as a butt weld of the usual type. Roberts[16] used a cantilever reversed-bend machine utilizing magnetic impulses synchronized with the natural frequency of the T. His specimens were welded with a bare electrode.

It is difficult to arrive at a basic value for the fatigue limit of fillet welds or T Joints, especially in view of the large effects of machining, shape and workmanship. At present, the published results indicate that both types of welds are inferior to butt welds.

Footnotes to Tables 1 to 4

St.w. = tensile strength of weld.
St.unw. = tensile strength of plate.

1. Ratio: Sf. w./ St. unw. = 0.29
2. " " " " = 0.50
3. " Sf.w./St.w. = 0.34
4. Cantilever machine
5. Ratio: Sf.w./St.w. = 0.34; Sf.w./St.unw. = 0.32
6. " " = 0.35; Sf.w./St.unw. = 0.34
7. " " = 0.33
8. " " = 0.40

Table 6—Results of Fatigue Test on T Joints

Type of Joint	Lower Stress S_u	Upper Stress S_o	Cycles to Fracture × 10^6	Fatigue Strength S_f
A. Thum and T. Lipp,[15] 1934 (Reversed Bend)				
Small Welded				12,800
Large Welded			B_k = 1.95–2.31	15,700
Cast Steel Large			B_k = 1.20–1.61	27,000
Cast Iron Large			B_k = 1.16–1.21	12,800
Forged Large			B_k = 1.58	17,100

B_k = Fatigue Limit of Polished Parent Metal in Rotating Bend / Fatigue Limit of Shape

R. Sulzer,[65] 1933 (Reversed Bend)		
1		23,500
2		21,400
3		22,800
4		28,600
5 Steel Casting		42,800
8 Special Cast Iron		24,200
A. Jünger,[43] 1930 (Reversed Bend)		
Steel 1		21,800
Steel 2		26,800

P. Schoenmaker,[132] 1936; Gerritsen,[132] 1936; Thierens,[131] 1935. (Pulsating Tension)

Lower Stress S_u	Upper Stress S_o	
12,500	23,100	S_T = 62,000
16,400	30,500	S_T = 58,800
15,100	28,000	S_T = 57,500
17,900	33,300	S_T = 57,400
19,000 (19,900	35,100 37,100 Gerritsen)	S_T = 57,400
17,000	31,500	

S_T = Static Tensile Strength, psi.

Table 6 (continued)—Results of Fatigue Test on T Joints

Type of Joint	S_u	S_o	Cycles to Fracture × 10^6	Fatigue Strength, psi S_f
M. Roš & A. Eichinger[5] (1936)				
Tension	0		>1	12,800–15,700
Tension	0		>1	8,600–10,000
Compression		0	>1	12,800
A. M. Roberts[16] 1935 (Reversed Bend)				
1				±17,900
2				±20,200
3 Unwelded				±24,600

Footnotes to Tables 1 to 4 (Continued)

9. Ratio: Sf.w/St.w = 0.66; Sf.w./St.unw. = 0.52
10. Cantilever machine
11. " " Sf.w./St.w. = 0.4
12. " "
13. Ratio: Sf.w./St.unw. = 0.28
14. " " = 0.27
15. " " = 0.13 to 0.41

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THE ENGINEERING FOUNDATION
WELDING RESEARCH COMMITTEE

Sponsored by American Welding Society and American Institute of Electrical Engineers

Issued as a Supplement to the Journal of the American Welding Society, February 1937

Welding Research Committee Activities

A meeting of the Welding Research Committee of The Engineering Foundation was held in New York on January 8, 1937. A temporary $20,000 budget was approved and steps taken to raise an additional $10,000, which will permit speedier progress and accomplishments of a number of additional desirable objectives.

The personnel of the Committee was enlarged and now includes the following:

C. A. Adams, *Chairman;* Consulting Engineer.

Everett Chapman, President, Lukenweld, Inc.

J. H. Critchett, Vice-President, Union Carbide & Carbon Research Laboratories.

J. J. Crowe, Engineer-in-charge of Apparatus Research and Development Department, Air Reduction Company.

Arthur S. Douglass, Construction Engineer, Detroit Edison Co.

C. L. Eksergian, Chief Engineer, Budd Wheel Company.

H. M. Hobart, Consulting Engineer, General Electric Company.

D. S. Jacobus, Advisory Engineer, The Babcock and Wilcox Company.

G. F. Jenks, Commanding Officer, Watertown Arsenal.

J. Jones, Chief Engineer, Fabricated Steel Construction, Bethlehem Steel Company.

P. G. Lang, Jr., Engineer of Bridges, Baltimore & Ohio Railroad.

F. T. Llewellyn, Research Engineer, United States Steel Corporation.

W. Spraragen, *Secretary;* Technical Secretary and Editor, AMERICAN WELDING SOCIETY, 29 West 39th Street, New York.

A. Weymouth, Vice-President and Chief Engineer, Post & McCord, Inc.

Fundamental Research

H. M. Hobart, on behalf of this subcommittee, reported a successful conference of professors interested in Fundamental Research, and publication during the year 1936 of ten research reports as follows:

Arc Welding in Argon Gas—Gilbert E. Doan and W. C. Schulte.

Tests to Determine the Feasibility of Welding the Steel Frames of Buildings for Complete Continuity—W. M. Wilson.

Advantages of Welding in Continuous Structures—Inge Lyse.

The Metallurgy of "Pure Iron Welds"—Gilbert E. Doan and W. C. Schulte.

Proposed Impact Test Specimen—A. Vogel.

The Effect of Low Temperature on the Tensile Impact Resistance of Iron, Steel and Welded Joints—Otto H. Henry.

Welded Structural Brackets—Cyril D. Jensen.

Electric Heating by the Proximity Effect—Edward Bennett.

Characteristics of a Universal Welding Generator—N. F. Ward.

Report on Some Impact Tests of Fillet Welds—Wendell F. Hess.

The Chairman reported that fifty active research projects were under way in many universities of the country from the East Coast to the West Coast and even into Canada.

Although some of these researches are obviously of minor importance they serve as a means of educating professors and students of the institution in question, thus providing a supply of young engineers who are familiar with welding. However, a considerable number of these researches have developed data, methods of analysis and information as to the nature of the phenomena involved, which, all told, constitute a major contribution to those industries concerned with welding. Altogether more than 100 important reports have been issued.

"A List of Fundamental Research Problems in Welding—Reviews of Recent Welding Literature," a pamphlet of 40 pages, was published as a supplement to the November JOURNAL and furnishes to all those interested a list of suggested problems needing solution, and a selected interpretive bibliography of current welding literature on some twelve important phases of welding.

Critical Digests of the Literature

The Fundamental Research Committee in cooperation with the Literature Committee reported completed reviews of the literature as follows:

Bend Testing of Welds—A Summary, by Professor M. F. Sayre, Union College, JOURNAL AMERICAN WELDING SOCIETY, December 1935.

Impact Tests of Welded Joints, by W. Spraragen and G. E. Claussen, JOURNAL AMERICAN WELDING SOCIETY, April 1936.

Fatigue Tests of Welded Joints, by W. Spraragen and G. E. Claussen, JOURNAL AMERICAN WELDING SOCIETY, January 1937.

Chairman Critchett reported that plans are under way for contractual arrangements with eight professors to undertake specific reviews of the literature on selected phases of welding.

Industrial Research

Colonel Jenks reported the organization of a number of subcommittees as follows:

The Material Subcommittees appointed are: Cast Iron—(Chairman not yet named); Carbon Steels—J. C. Hodge, *Chairman;* Low-Alloy Steels—J. H. Critchett, *Chairman;* High-Alloy Steels—T. H. Nelson, *Chairman;* Aluminum Alloys—G. O. Hoglund, *Chairman;* Copper Alloys—D. K. Crampton, *Chairman;* Nickel Alloys—O. B. J. Fraser, *Chairman.*

The Functional Subcommittees are: Methods of Testing—M. F. Sayre, *Chairman;* Analysis of Weld Failures—(Chairman not yet named); Weld Stresses—Causes and Effects—E. Chapman, *Chairman;* Non-Destructive (Radiographic) Tests—J. T. Norton, *Chairman.*

One of the important functions of this committee is the stimulation of research through participation in technical programs, and publication of papers in technical periodicals. One such report was published in the October 1936 issue of the JOURNAL entitled, "Arc Welding of Structural Alloy Steels" by W. Warner, and another on "Welding of Copper, Nickel and Low-Alloy Steels" by A. E. Gibson in this issue of the JOURNAL.

Publications

Arrangements were made for the use of THE WELDING JOURNAL as the official organ of the Committee. It is planned to publish the Supplement to this JOURNAL each month, which will be devoted to the activities of the Committee.

Translations

American investigators are generally handicapped by lack of knowledge of the foreign languages. The Fundamental Research Committee in cooperation with many others, has established a mutually cooperative translation plan whereby translations of important foreign articles are made available to those interested. Some 16 companies, or individuals, are now cooperating in this plan. Some furnish translations, others assist in supplying mimeographed copies of translations made by others and still a third group does both. Announcement of the availability of these translations are made in the JOURNAL. In this way some thirty important translations have been made generally available. In a few cases only file copies have been supplied, which are lent upon request. But in most instances the Committee has been able to supply without charge copies to those who have indicated a genuine interest in these translations.

The Nickel-Copper High-Strength Steels for Welded Construction*

By ALFRED E. GIBSON†

THE important place which high-strength steel is taking in the fabrication of equipment is evident on every hand. The decision as to which particular low-alloy steel is best suited for a certain fabrication is often perplexing. While the national advertising of steel producers is based on facts, it is often confusing to the engineer or fabricator who is not familiar with the metallurgy of steel and the predictable reactions of the commonly used alloys. A large amount of low-alloy steel finds its way into welded structures and this article will deal with the properties of low-alloy nickel-copper steel for welding.

The main requirements of a low-alloy high-strength steel are high yield and ultimate strength but there are other physical and metallurgical considerations of great importance, often more important than maximum yield and ultimate strength. Advertisements tell us that low-alloy steels possess certain high strengths. The physical properties given are always the results of tests of as-rolled steel. This is probably so because the entire experience of the steel industry, at least until the last few years, has been with structural steel fabricated by riveting. In the development of a high-strength steel, the metallurgist has been riveting minded and not welding minded. Too much reliance has been placed on the properties of the material as rolled and too little on the material when welded.

Welding Has a Bearing on Properties

Scant consideration has been given to the fact that in riveted construction only slight metallurgical reactions take place, while in welding drastic metallurgical and metallographical reactions are encountered, due to the high temperatures used in depositing metal. Many variables affect the resulting weld, the most troublesome physical property encountered being air hardening. The degree of air hardening is greatly affected by certain alloys used to produce high strength. Hence, there are many instances where it is preferable to sacrifice strength in order to lower the air hardening properties of the steel.

The question, "What is an ideal low-alloy high-strength steel for welding?" could be answered by stating the chemical and physical properties of every commercially produced low-alloy steel. There is no one analysis, no one combination of alloys, which fulfills the requirements for an ideal steel under every condition. If, as is often the case, the essential requirement is resistance to abrasion, many steels ideal under certain conditions would fall far short of this ideal. If resistance to atmospheric corrosion is the chief requirement, some alloys admirable under other conditions would not be suitable. To answer that question we will limit the requirements to an ideal steel for the welded structures which form a large volume of our mechanical fabrications. The author's specification of an ideal steel is one having a high yield and ultimate strength, the former as near the latter as possible, or in more technical terms, possessing a high elastic ratio, without undue loss of ductility; a steel with high resistance to fatigue and impact; one capable of being welded with the same equipment, the same electrodes, the same technique as used with low-carbon steel; a steel having several times the resistance to atmospheric corrosion of low-carbon steel; a steel capable of having its yield and ultimate strength materially increased by simple heat treatment without dangerous loss of impact strength; *a steel having low air hardening property when welded or gas cut.* A difficult specification but not an impossible one.

Nickel-Copper Alloys

The nickel-copper low-alloy steels meet the above conditions. The importance of copper as an alloy imparting high strength is evident from the fact that an 0.08 per cent carbon, 2.00 per cent nickel stay bolt steel has a yield of about 35,000 psi and 50,000 psi ultimate strength but when 1.00 per cent copper is added to the same analysis, the yield point is increased 60 per cent, the ultimate strength 30 per cent.

The nickel-copper steels are commercially produced and nationally advertised by three leading steel manufacturers. Several others are prepared to furnish them on special order. Each of the three mills produces this steel with different percentages of nickel and copper, one with a nickel-copper ratio of 2 to 1, another $^3/_4$ to 1 and the third 1 to 2. In each case the copper content is at least 1.00 per cent. This is so because the value of copper as a strengthening alloy is not effective below 0.7 per cent. One of these steels is produced with one carbon content only, 0.10 per cent, the other two, with carbon ranges approximately 0.10 per cent and 0.20

* Vice-President, Wellman Engineering Co.
† Article Contributed to the Industrial Research Subcommittee. Reprinted from *Steel*.

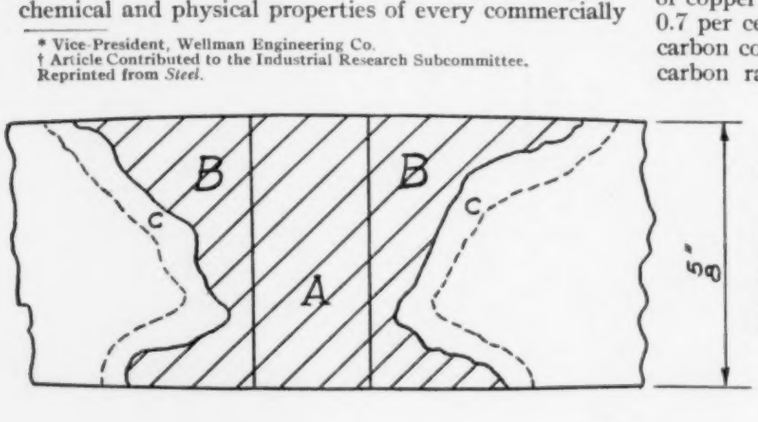


Fig. 1—Analysis of Nickel-Copper Plate and Weld Using Low-Carbon Electrode with 0.70 Per Cent Molybdenum in the Coating

Region	Carbon, Per Cent	Nickel, Per Cent	Copper, Per Cent	Molybdenum, Per Cent
A—Weld	...	0.67	0.43	0.21
B—Weld	...	1.01	0.52	0.20
C—Plate	0.24	1.98	1.01	...
A—Weld	...	0.71	0.42	0.29
B—Weld	...	0.89	0.46	0.24
C—Plate	0.08	1.90	1.05	...

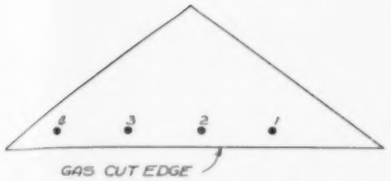


Fig. 2—Rockwell B Hardness Values of 0.17 C, 1.95 Ni, 1.00 Cu Steel Plate after Torch Cutting and after Reheating with Torch to Anneal

Location	Hardness after Gas Cutting	Hardness after Torch Drawing
1	102	90
2	109	90
3	100	91
4	92.5	90

Hardness of Plate as Rolled 90

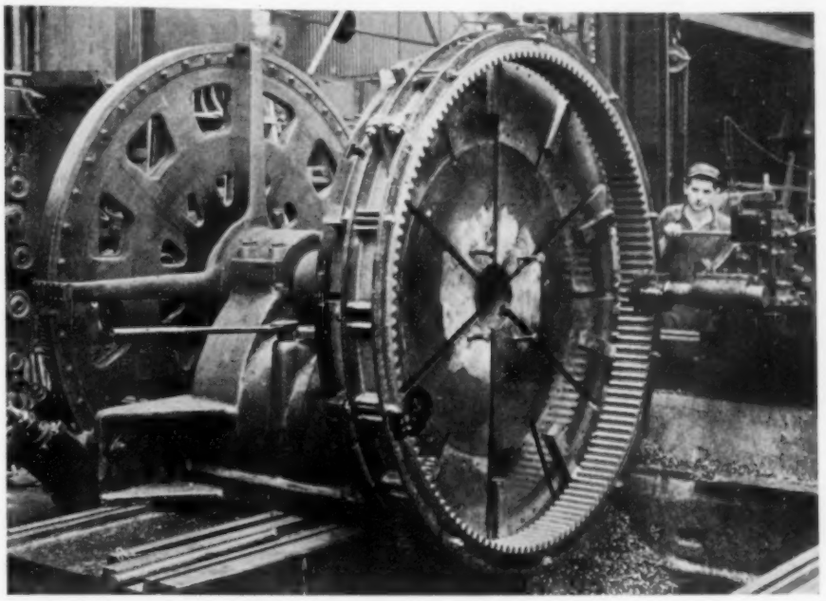


Fig. 3 (Right)—One of Six 80-Inch Pitch Diameter Internal Gears of Nickel-Copper Steel Welded Construction, Heat-Treated after Welding for Stress Relief

per cent. Naturally the higher carbon content produces steel of increased yield and ultimate strength but as carbon is a most active hardening element, it affects the air hardening quality markedly.

In welded constructions we are interested in the weld strengths procurable. In certain types of welded joints in low-alloy steel, it is possible to increase the section of the weld so that its total strength equals or exceeds that of the parent metal. In many instances it is not possible to have a greater section of weld than plate. Therefore, if welds are to have as great strength as the rolled steel, it is necessary to have the filler metal alloyed. This is made possible by the use of alloy electrodes or of low-carbon electrodes having alloys in the coating. Either method is effective. All of the alloy need not be in the electrode or coating because the filler metal becomes alloyed from intermixture with the molten parent metal. The smaller the weld, the greater the percentage of parent metal alloy found in the weld. This intermixture is shown in Fig. 1; using $^5/_8$-inch plate, with a double-V butt weld, the latter contained from $^1/_3$ to $^1/_2$ the percentage of alloy found in the rolled plate.

Properties of Copper-Nickel Steels

Table 1 lists several copper-nickel steels, the chemical and physical properties of the plate as-rolled, and the values of the welds, as-welded, stress-relieved, normalized and normalized precipitated. Under the last treatment is shown the greatly increased yield and ultimate strength produced. While to-date little practical application has been made of the property of precipitation treatment, it is an intensely interesting phenomenon. The danger of introducing objectionable cooling strains in large welded structures with varied sections by rapidly cooling in air is realized. In simple welded details of fairly uniform section, few ill effects would result.

Metallurgists tell us that a currently accepted metallurgical hypothesis states that only about 0.35 per cent of copper is held in solid solution at room temperature if the steel is slowly cooled, and that copper in excess of this amount may precipitate out of solution. When the copper content is greater than 0.70 per cent and the steel is heated to about 900 to 1000° F. for one to four hours and cooled, the yield and ultimate strengths of the steel are greatly increased. Copper steels are the only common low-alloy steels which when heated

Table 1

Copper-Nickel Steel

	C		Ni		Copper	
	0.08		1.99		1.02	
	Yield Weld psi	Yield Parent psi	Ult. psi	% El. 2 In.	% Red. area	Location Facture Break
As-Welded	57,000		63,000	29	70	Parent Metal
As-Welded	65,000		69,000	26	66	" "
As-Welded		55,800	65,300	33	54	" "
As-Welded		55,800	65,900	34	56	" "
950° F. 1 Hr.	80,000		83,000	24	57	" "
950° F. 1 Hr.	72,000		84,000	20	37	Weld Metal
950° F. 1 Hr.		63,500	80,000	31.5	51	Parent Metal
950° F. 1 Hr.		63,400	80,400	31	52	" "
1200° F. 1 Hr.	57,000		71,000	29	64	" "
1200° F. 1 Hr.	70,000		73,000	29	64	" "
1200° F. 1 Hr.		68,100	71,500	31	51	" "
1200° F. 1 Hr.		63,000	70,900	25.5	54.5	" "
1650° F. 1 Hr.	55,000		68,000	35	62	" "
1650° F. 1 Hr.	53,000		68,000	33	60	" "
1650° F. 1 Hr.		51,400	65,600	42.5	59.5	" "
1650° F. 1 Hr.		50,600	65,600	40	63	" "

Copper-Nickel Steel

	C		Ni		Copper	
	0.22		1.98		0.92	
	Yield Weld psi	Yield Parent psi	Ult. psi	% El. 2 In.	% Red. area	Location Fracture Break
As-Welded	85,600	72,000	95,400	27	42	Parent Metal
As-Welded	85,200	66,300	91,800	27	44	" "
950° F. 1 Hr.	82,600	85,000	90,300	9	16.6	Weld, Slag, Inc.
950° F. 1 Hr.	78,500	94,000	100,800	18.5	32.6	Weld
1200° F. 1 Hr.	69,300	78,800	88,300	16.5	24	Weld
1650° F. 1 Hr.	64,400	69,000	81,300	17.5	38	Weld
1650° F. 1 Hr.	65,300	70,300	79,750	14	14.5	Weld, Slag, Inc.

Copper-Molybdenum Steel

Average of 5 Test Specimens

	C		Molyb.		Copper	
	0.25		0.25		1.40	
	Yield Weld psi	Yield Parent psi	Ult. psi	% El. 2 In.	% Red area	Location Fracture Break
As-Welded	79,870	90,760	93,740	28	180	Weld
950° F. 1 Hr.	88,390	95,270	98,740	31	141	Weld

Copper-Nickel-Molybdenum Steel

	C	Ni	Copper	Molyb.
	0.23	0.75	1.30	0.16

	Yield Weld psi	Yield Parent psi	Ult. psi	% El. 2 In.	% Red area	Location Fracture Break
As-Welded	80,590	90,880	96,460	29	149	Weld
950° F. 1 Hr.	93,870	102,920	104,620	28.8	147	Weld

Elongation and Reduction are approximate as test specimens were rectangular. Distinct yield points for weld and parent metal were indicated by drop of the beam in testing.

to this temperature range will thus increase in strength. This increase is frequently accompanied by corresponding loss in impact strength as determined by the notched bar test. As is true of most steels, normalizing improves the impact strength. If it is desired to use them in the precipitation hardened condition, the normalizing should precede precipitation hardening. (See Table 2.)

The nickel-copper steels have excellent properties of atmospheric corrosion resistance, four to six times that of low-carbon steel. Welds with low-carbon electrodes contain one-third to one-half or more per cent of copper due to intermixture with the molten parent metal and come under the classification of copper steels, used for years as corrosion-resistant steels.

Nickel-copper steel welded with low-carbon heavily coated electrodes having approximately 0.70 per cent molybdenum in the coating show the following analysis:

Plate 2¼ inch thick, single channel groove

	C	Mn	S	P	Si	Ni	Cu	Mo
Plate	0.20	0.80	0.028	0.010	0.18	1.80	0.90	..
Weld	0.08	0.50	0.02	0.015	..	0.50	0.33	0.70

Tests made by the Champion Rivet Company with nickel-copper coated electrodes and plate produce strikingly uniform analysis:

	C	Mn	S	P	Si	Ni	Cu
Plate and Electrode	0.06	0.40	0.008	0.053	0.003	2.01	1.01
Weld	0.095	0.53	0.004	0.053	0.23	1.86	1.00

The increase in carbon, silicon and manganese is from these elements present in the coating.

In corrosive-resistant equipment, welds in nickel-copper steel made with electrodes having the same analysis as the parent metal will without a doubt come into general use, especially as tension and bend properties of the stress-relieved welds are almost identical with those of the parent metal. F. J. Esslinger reports (AMERICAN WELDING SOCIETY JOURNAL—Jan. 1936) the following results. Butt welded 0.08 per cent and 0.20 per cent carbon, 2.00 per cent nickel, 1.00 per cent copper steel plate and electrode gave the following tension and bend test results:

Tests of Copper-Nickel Plate

Tension Tests

	0.08% C, Ni-Cu		0.20% C, Ni-Cu	
	Yield psi	Ult.	Yield psi	Ult.
As-Welded	58,000	70,000	66,000	91,000
1200° F. Heat Treat	62,000	73,000	69,000	92,000
1580° F. Anneal	63,000	74,000	68,000	90,000

Note: All fractures in rolled plate.

Bend Tests

	Bend	Bend	Elong.	Fracture
As-Welded	180°	45°	14%	Complete
1200° F. Heat Treat	180°	180°	31%	3/16-in. crack in weld
1580° F. Anneal	180°	180°	32%	3/16-in. crack in weld

All Weld Metal Tension Specimens

	Yield psi	Ult.	Elong. %	Red. area	Yield psi	Ult.	Elong. %	Red. area
As-Welded	58,500	80,000	24	37	69,000	95,000	13	12
1200° F. Heat Treat	70,000	81,000	27	54	76,000	91,000	18	20
1580° F. Anneal	64,000	77,000	26	53	69,000	87,000	17	20

These tests prove the high physical values of welds made with the same analysis nickel-copper electrode as the plate and that the ductility of the low-carbon welds is far in excess of the code requirements for low-carbon unalloyed plate.

A recent investigation of the comparative loss by corrosion of 0.22 carbon, 2 per cent nickel and 1 per cent copper pipe in comparison with ordinary black pipe, when subjected to an 85 per cent saturated brine solution for a period of 615 hours, showed the latter to corrode from two to four times as fast as the nickel-copper alloy, depending upon whether the pipe is submerged in the brine or in contact with the vapor only. The latter condition is the most severe. (Report of Denver Gardner Laboratory.)

One of the chief advantages in the use of the nickel-copper steels is the low degree of air hardening experienced in welding and gas cutting operations. The degree of hardening is not such as to seriously affect the ductility of the material or to present any difficulty in machining operations. In butt welding heavy sec-

Table 2—Effect of Heat Treatment on Copper-Nickel Steel

Chemical Analysis

C	Mn	S	P	Si	Ni	Cu
0.22	0.67	0.020	0.048	0.26	1.97	0.92

Physical Properties

Treatment	Yield Point psi	Ult. Str. Lb./Sq. In.	El. % in 2 In.	Reduction in Area	*Charpy Impact	"B" Scale Rockwell Hardness	Brinell Hardness
Hot Rolled	61,000	88,000	27	57	25	89	162
Norm. at 1450° F.	68,000	92,000	28	58	32	89	159
Norm. at 1650° F.	71,000	92,000	28	58	33	89	160
Norm. 1650° F.—Renorm. 1450°	72,000	93,000	28	58	34	87	160
Norm. 1650° F.—Renorm. 1375°	66,000	89,000	28	58	35	89	162
Norm. 1800° F.—Renorm. 1450°	61,000	87,000	28	58	29	88	166
H. R. and Heated 900° F.—4½ Hr.	82,000	107,000	25	54	16	95	196
Norm at 1450° F.—Heated 900° F.—4½ Hr.	82,000	105,000	25	54	23	93	202

* Impact test specimens 0.250 inch x 0.394 inch, slotted by milling, leaving 0.197 inch x 0.394 inch metal under the notch which has a radius of 0.02 inch.

Physical properties of nickel copper steel under various heat treatments.

Table 3—Charpy Impact Tests—"V" Notch

2% Ni—1% Cu—0.17% C Steel

Plate	Heat Treatment	Position of Notch	Impact Value in Ft.-Lbs.	Rockwell B
⅞-in.	As Received	Parent Metal	44	
⅞ in.	As Received	Parent Metal	43	
⅞ in.	As Received	Parent Metal	31.5	
Average			39.5	90
⅞ in.	1650° F. one hour, air cooled	Parent Metal	116.5	
⅞ in.	1650° F. one hour, air cooled	Parent Metal	93.5	
⅞ in.	1650° F. one hour, air cooled	Parent Metal	93.5	
⅞ in.	1650° F. one hour, air cooled	Parent Metal	110	
Average			103.4	90.5
⅞ in.	1650° F. one hour, air cooled	Parent Metal	94.5	
⅞ in.	reheat to—	Parent Metal	98.5	
⅞ in.	950° F. one hour, air cooled	Parent Metal	71	
⅞ in.		Parent Metal	81	
Average			86.3	94
2¼ in.	Welded with Electrode "A"	Weld	63.5	
2¼ in.	Welded with Electrode "A"	Weld	61.5	
Average			62.5	
2¼ in.	Welded with Electrode "A"	Diffusion Zone	75	
2¼ in.	Welded with Electrode "A"	Diffusion Zone	77.5	
Average			76.3	
⅞ in.	As-Welded	Weld	46	
⅞ in.	Nickel-Copper Electrode "B"	Weld	41	
Average			43.5	91.5
⅞ in.	As Welded	Diffusion Zone	96.5	
⅞ in.	Nickel-Copper Electrode "B"	Diffusion Zone	75	
Average			85.8	95
⅞ in.	1250° F. one hour, air cooled	Weld	50	
⅞ in.	Nickel-Copper Electrode "B"	Weld	52.5	
Average			51.3	87
⅞ in.	1250° F. one hour, air cooled	Diffusion Zone	146.5	
⅞ in.	Nickel-Copper Electrode "B"	Diffusion Zone	143.5	
Average			145.0	95.3
⅞ in.	1250° F. one hour, air cooled	Parent Metal	60	
⅞ in.	Nickel-Copper Electrode "B"	Parent Metal	51.5	
Average			55.8	90.5

Electrode "A"—Low Carbon, 0.70% Moly. in coating
Electrode "B"—Low Carbon, 2.00% Ni, 1.00% Cu

tions which have been shaped by gas cutting, it is necessary to machine channel grooves for complete penetration. The necessity of annealing after gas cutting and before welding, due to excessive air hardening, is eliminated with the nickel-copper steels.

High Physical Values.—The high yield and ultimate strength of the nickel-copper steels in sheets and light plate has been repeatedly demonstrated. The value of these properties in heavy plate was until recently a matter of conjecture. The Wellman Engineering Company have lately designed and fabricated a number of 80-inch pitch diameter internal gears with 7 inch face, and also 30 inch diameter by 34-inch face drums of welded construction. Light weight was imperative so welded designs of low-alloy steel were selected. The analysis specified was maximum 0.20 per cent carbon, maximum 0.80 per cent manganese, approximately 2.00 per cent nickel and 1.00 per cent copper. Although the metallurgical department of the mill to which the order was given would not guarantee physical properties above 50,000 psi yield strength and 70,000 psi ultimate strength, the author was confident that much higher physical values would be obtained. The actual chemical and physical properties obtained were: 0.17 per cent carbon, 0.74 per cent manganese, 2.00 per cent nickel, 1.00 per cent copper, yield point 66,400 psi, ultimate strength 86,600 psi and 18.5 per cent elongation in eight inches. These physical values were obtained with rectangular test pieces in the as-rolled condition. Standard 0.505 inch round test specimens cut from the center and outside of the plate produced 69,180 yield and 90,700 ultimate strength with 25 per cent elongation in two inches. The gears and drums, six of each, were stress relieved at 1200° F. after welding, the former held for two hours and the latter for one hour. The maximum thickness of plate was $2^1/_4$ inch in the gear and $^7/_8$ inch in the drums. The weld strengths obtained, while not as high as those of the parent metal, are of a high order. These high-weld strengths were secured with electrodes having 0.70 per cent molybdenum in the coating. Butt welded tensile test pieces of $2^1/_4$-inch plate with double channel weld grooves were made. The plate was then sawed and the test pieces machined all over approximately $^3/_4$ inch by $1^7/_8$ inch. The physical values obtained were as follows:

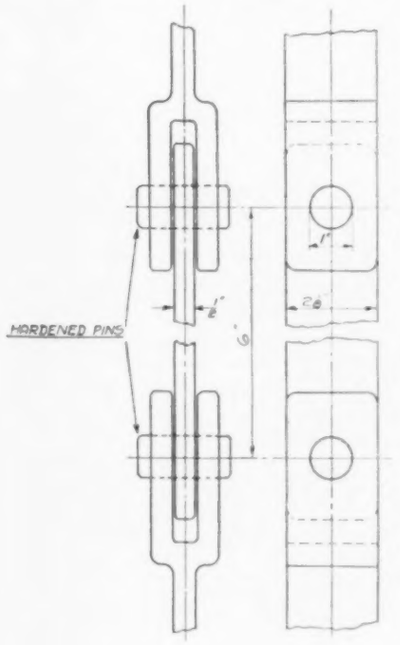


Fig. 4—Tensile and Hardness Values of Nickel-Copper Steel as Rolled and after Flame Hardening

	Tensile Strength, Pounds per Square Inch
As-Rolled—Low-Carbon Steel	61,900
As-Rolled—0.20% C, 2.00% Ni, 1.00% Cu Steel	88,300
After Flame Hardening—0.20% C, 2.00% Ni, 1.00% Cu Steel	114,000

Distance from Edge of Hole, Inches	Flame Hardened Surface of Bar Rockwell C
1/16	43
1/8	42
3/16	42
1/4	40.5
5/16	38
3/8	34
7/16	28.5
1/2	24
9/16	18.5
Unhardened Plate	7.8 (87 Rockwell B)

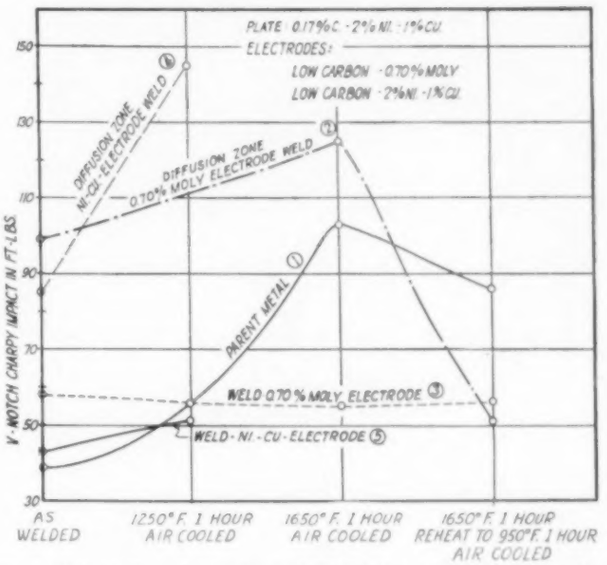


PLATE 0.17%C - 2% NI. - 1% CU.
ELECTRODES:
LOW CARBON - 0.70% MOLY
LOW CARBON - 2% NI. - 1% CU.
DIFFUSION ZONE NI-CU-ELECTRODE WELD
DIFFUSION ZONE 0.70% MOLY ELECTRODE WELD
PARENT METAL
WELD 0.70% MOLY ELECTRODE
WELD - NI - CU - ELECTRODE
V-NOTCH CHARPY IMPACT IN FT-LBS
150
130
110
90
70
50
30
AS WELDED
1250° F. 1 HOUR AIR COOLED
1650° F. 1 HOUR AIR COOLED
1650° F. 1 HOUR REHEAT TO 950° F. 1 HOUR AIR COOLED

Fig. 5—V-Notch Charpy Impact Curves of Welded Nickel-Copper Steel Using Two Types of Electrodes

Yield of Weld psi	Ult. of Weld psi
As-Welded	
67,500	82,100
67,400	78,800
Normalized 1600° F. Air Cooled	
61,400	75,100
62,200	74,200
65,600	73,500

All fractures occurred in the weld and are therefore weld strengths and not parent metal strengths. It is interesting to note that the Rockwell hardness tests showed strikingly little difference in the hardness of the weld, affected zone and parent metal. Rockwell B values of the weld varied from 87 to 92. After normalizing, these values were 70 to 88 and after being normalized and precipitated, 75 to 91.

Rockwell hardness of the parent metal cut with a gas torch indicates the low air hardening attending this operation, of importance when machining operations

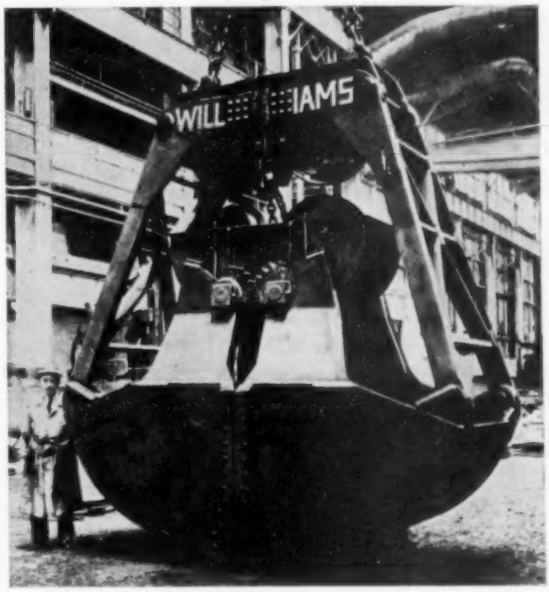


Fig. 6—This 8-1/2 Cubic Yard Nickel-Copper Steel Bucket Was Designed to Handle 7 Tons of Coal. However, It Is Actually Picking Up 8 Tons per Grab

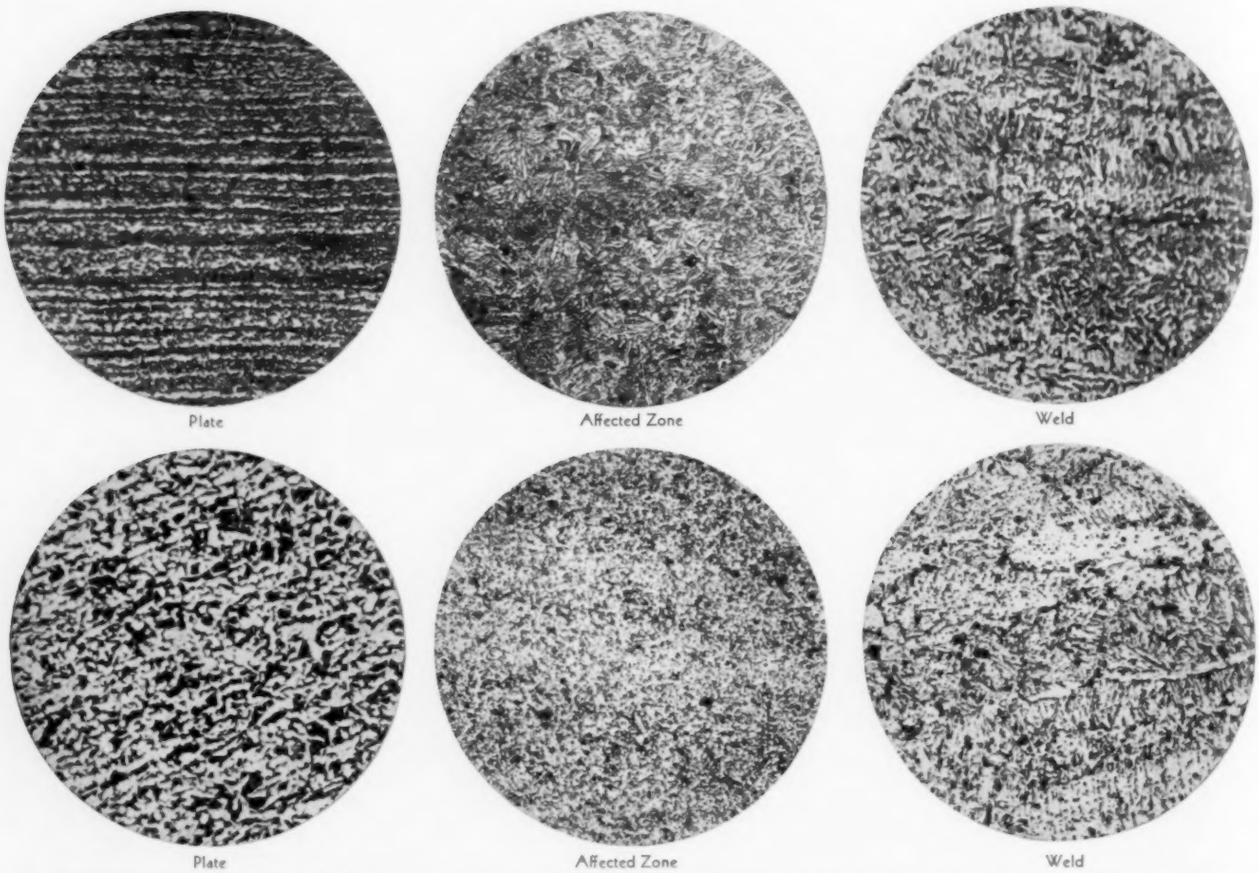


Fig. 7—Photomicrographs of Nickel-Copper Steel and Welds. X 100. The Top Row Is for Steel Containing 0.23 C, 0.75 Ni, 1.40 Cu and 0.08 Mo; the Bottom Row for Steel Containing 0.22 C, 2.00 Ni and 1.00 Cu

for weld grooves are to be performed. Figure 2 shows Rockwell B readings after torch cutting and when the residual hardness is lowered by subsequent reheating with a torch. It has been our experience that most of the air hardening may be relieved by playing a heating torch on the metal after gas cutting.

Can Be Hardened

The nickel-copper steels can be very satisfactorily hardened by quenching or flame hardening. Steels with 0.20 carbon, 2.00 per cent nickel, 1.00 per cent copper rolled into $^3/_4$ inch bars has a Rockwell B hardness of 91–93. When heated and water quenched, it has a surface hardness of 45 Rockwell C which converts to 115 Rockwell B.

The results of tensile tests of low-carbon steel, of 0.20 per cent carbon nickel-copper steel as-rolled and of the latter steel after flame hardening around the 1-inch holes through which the stress was applied, are shown in Fig. 4. The nickel-copper steel as-rolled was 42.6 per cent higher in tensile strength than the low-carbon steel. Flame hardened nickel-copper steel was 96.8 per cent higher than the low carbon and 28.8 per cent higher than the as-rolled low alloy. This is of particular interest in riveted structures as the heat of driven rivets affect the structure of the steel in the same manner as flame hardening. (See High Tensile Rivets of Cromansil Steel—Magazine "Steel"—October 2, 1933.)

Charpy Impact.—Table 3 gives the charpy "V" notch values of the plate and welds of approximately 0.20 carbon nickel-copper steel as-welded and under varying heat treatments. Figure 5 graphically shows the charpy values with two types of electrodes and under various heat treatments. The impact value of the weld remains fairly constant whether subsequently heat treated or not while the value of the parent metal and the affected zone increase markedly under thermal stress relief and normalization.

From a welding standpoint, the nickel-copper steels are safe. Their air hardening property is low, a great advantage in case of needed welding in service. They have the highest elastic ratio of any of the commercially produced low-alloy steels. This fact will be decidedly to its advantage when engineers come generally to substitute yield for ultimate strength in figuring sections. The author refers you to the *Mining and Metallurgical Engineers Technical Publication*, 697 and to Hargh, "Fatigue of Structural Steel," *Engineering* (1934) 138, 698–701.

The high impact value of the nickel-copper steels recommend them for service where shock is encountered. They offer no complication in welding with either low-carbon or alloyed electrodes.

The author wishes to acknowledge the assistance of the Case School of Applied Science, Cleveland, Ohio, where a considerable part of the physical and metallographic investigations were made.

The Welding of Copper and Its Alloys—A Review of the Literature to Jan. 1, 1936

By IRA T. HOOK†

Foreword

As a part of the work of the Welding Research Committee (Fundamental Research and Literature Subcommittees) sponsored by the American Society of Electrical Engineers and the AMERICAN WELDING SOCIETY *under the auspices of the Engineering Foundation, the following critical review has been made. We find comparatively little information concerning the welding of copper alloys earlier than 1914. Indeed most of it is the accumulation of the last ten years. While we realize that much excellent work has been done in foreign language countries which is not reviewed herein, an attempt has been made to give a partial review of the more important work published in German French, Italian and Swedish. The English language field has been covered rather more thoroughly.*

Plan of Review

THE object of the review is to summarize our present knowledge of the art of welding as it applies to the copper alloys with the view of planning further researches without duplication of previous accomplishments.

In order to facilitate the finding of information on the welding characteristics of any particular commercial copper alloy, the discussion is divided into eight general parts. The several different methods of welding are arranged as sub-divisions. For the most part, we have ignored metallurgical or chemical information that has no direct bearing on the welding properties of the alloys in question.

Aside from Part 1 which deals with the general metallurgy of the copper alloys and Part 8 which is concerned with special welding processes involving copper alloys, the welding information available on the usual commercial copper alloys is given in Parts 2 to 7, inclusive. In each of the six groups, the welding characteristics of the alloys discussed in a given group are somewhat similar. Thus:

PART 1: General Metallurgy of Copper.—Effect of small additions of other elements on the welding properties of copper as a base metal and as a weld metal.

PART 2: Copper, Deoxidized Copper.—Welding processes in the literature.

PART 3: Copper-Zinc Alloys.—Welding properties of brass and nickel silver, extruded brass and bronze, Muntz metal, Tobin bronze, Manganese bronze, leaded brass and bronze and cast brass.

PART 4: Copper-Tin Alloys.—Welding properties of Phosphor bronze, Gun metal, Bell metal, Bearing bronzes, etc.

PART 5: Copper Silicon Alloys.*—Welding properties of Everdur and similar commercial alloys of copper and silicon with manganese, zinc, tin, etc.

PART 6: Copper Nickel Alloys.—Welding properties of copper-nickel alloys having nickel less than 50%, the cupro-nickels.

PART 7: Copper-Cadmium, Copper-Aluminum, Copper-Beryllium Alloys.—Grouped together on account of their similar welding characteristics though quite unlike chemically.

PART 8: Miscellaneous Welding Processes Involving Copper Alloys.—Spelter-solder brazing, copper brazing, bronze welding, bronze coating, etc.

CONCLUSION: The review is concluded with a glance at the dark spots in the art with suggestions as to how these may be illumined.

PART 1

General Metallurgy of Copper as It Applies to Welding

In order to obtain a proper understanding of the correct procedures and difficulties encountered in the welding of copper, a brief exposition of the properties of pure copper and copper as affected by other elements is essential. Copper as it is usually made and sold is one of the purest metals handled in the ordinary course of commerce. The ordinary copper used in copper tubes, copper wire, copper sheet and plate for a multitude of purposes is better than 99.9% pure copper.

This high purity is required since 60% of the copper produced goes into the electrical trade where electrical conductivity is of vital importance. And in order to maintain the high electrical conductivity, approximating 100% of the International Annealed Copper Standard, a high chemical purity of the metal is required. A few hundredths of one per cent of some alloying elements may reduce the electrical conductivity to a quarter of its normal value. This fact is as important to the weldman as it is to the electrical designer as there is a direct relationship between the electrical conductivity and the thermal conductivity. The latter has been shown for all of the copper alloys[1] to be very nearly proportional to the product of the electrical conductivity and the absolute temperature.

Properties of interest in welding.—Some of the properties which are of vital importance in the welding art and which, therefore, are dealt with herein are [2,3]:

(*a*) Melting point and boiling point or vaporization temperature of the metal.

(*b*) Affinity of the metal for oxygen at various temperatures.

(*c*) Relative solubility of the metal oxide (*m*) in the metal and (*n*) in the available fluxes. Reactions of one oxide upon another and with the flux.

(*d*) Melting point or vaporization temperature of the metal oxide.

(*e*) Density of the oxide as compared to that of the metal.

(*f*) Surface tension of the molten metal and its viscosity** or internal friction. These properties ordinarily

* Parts 5 to 8 inclusive will be published in March 1937 issue.

† Research Engineer, The American Brass Company.

** The weldman prefers to look at this property from the other end of the scale speaking of it as "relative fluidity."

cannot be evaluated separately in terms of weldability as they are inextricably involved with similar properties of the oxides and fluxes.

(*g*) Ductility of the metal at all temperatures from its melting point to room temperature.

(*h*) Coefficient of thermal expansion (contraction to the weldman)—average for all temperatures below the melting point. The usual coefficient at room temperature to 200° C. is of interest as it has a bearing on the locked-up stress. The coefficient at or near the melting point is even more important to the weldman since it has a direct bearing on the danger of hot short cracking. Few values of these high temperature coefficients are available.

(*i*) Thermal conductivity of the base metal. Not so important for the weld metal as the latter is usually melted in small pieces.

(*j*) Effect of time on the welding reactions.

Deoxidizers for Copper.—The first and perhaps the most important rôle of the added element to a copper base metal or welding wire is that of a deoxidant. Phosphorus is without question the most convenient element which can be used for this purpose. The percentage of phosphorus required to deoxidize the welding wire thoroughly with sufficient residual phosphorus to deoxidize the weld metal under normal *gas* welding conditions is about 0.045%. The deoxidizing power of the various elements is evaluated by Geldbach,[4] column seven, Table 1 in terms of this amount of phosphorus. Some of the elements for one reason or another are not deoxidizers for copper.

There is considerable latitude for difference of opinion as to the relative effectiveness of copper deoxidizers. The reviewer offers, for purposes of furthering discussion, the following list, placing the elements in order of their relative commercial importance at this writing:

Phosphorus, Silicon, Manganese, Zinc, Aluminum, Magnesium, Beryllium, Chromium, Cadmium, Boron, Barium, Lithium, Calcium, Titanium, Hydrogen and Carbo-hydrogen gases.

Major Alloying Elements.—One of the valuable characteristics of copper is its capacity for alloying with other elements. Thus, by suitable additions wide variations can be obtained in color, strength, hardness, ductility, corrosion resistance, electrical, chemical and thermal properties. The alloying elements discussed herein placed roughly in order of their commercial importance are:

Zinc, Tin, Nickel, Silicon, Aluminum, Beryllium, Cadmium, Silver, Lead, Arsenic, Chromium, Iron and Phosphorus.

It will be noted from the two lists above that zinc, silicon, aluminum, beryllium, cadmium, chromium and phosphorus appear in a dual rôle, as deoxidizer and alloying element.

Effect of Small Additions on the Welding Properties of Copper.—A great deal of study has been given to the effect of small additions of other elements to copper though the work on this subject is by no means complete. One of the valuable contributions to the art is a paper by Wilhelm Geldbach[4] which, besides being an excellent review of the literature on copper welding to 1931, shows his observations on the effect of small additions of a great variety of elements on copper welding wires as they are melted by the oxyacetylene torch. The elements which he investigated and some of their properties are given in Table 1.

In the following discussion, the reviewer will consider the effect of each of the elements separately on the welding properties of copper when used as *small* additions only. Some of them are considered further in parts two to eight inclusive as major alloying constituents. Of the elements discussed, only the first three, namely, *oxygen*, *arsenic* and *phosphorus* are commercially important at this writing as additions to the *copper base metal*—sheets, tubes, bars and castings. All of the elements, including the first three, are discussed as possible additions to the copper welding rods.

The elements arranged roughly in order of their commercial importance at the present time are discussed as follows. Arsenic, in second place, is perhaps overrated but is given that position as it is the only alloying element commonly used in the copper base metal. 1. Oxygen, 2. Arsenic, 3. Phosphorus, 4. Silicon, 5. Silver, 6. Zinc,

Table 1—Physical-Chemical Constants of Added Elements Investigated. (Geldbach)

	Element	Melting Point, ° C.	Oxide	Specific Density of Oxide	Melting Point of Oxide, ° C.	Heat of Formation Calories	Equivalent in Deoxidizing Value to 0.045 to 0.050 Phos.	Melting Point Lowering of the Equivalent
1	Aluminum	658	Al_2O_3	3.85	2020	128	0.031–0.034	2°
2	Antimony	630	Sb_2O_3	5.20	656	55	0.175–0.19	..
3	Arsenic	817*	As_2O_3	3.718	subl.	52	0.11 –0.12	..
4	Beryllium	1300	BeO	3.06	2525	144	0.02 –0.022	..
5	Lead	327.4	PbO	9.3	879	50.8	.....	..
6	Boron	2300	B_2O_3	1.79	577	90.9	.....	..
7	Cerium	635	CeO_2	7.3	1950	...	.....	..
8	Chromium	1520	Cr_2O_3	5.04	1990	89	0.075–0.08	..
9	Iron	1514	FeO	5.12	1565	64.0	.....	..
10	Cadmium	321	CdO	8.15	1385	66	0.25 –0.27	2–3°
11	Calcium	803	CaO	3.15	1995	86.8	.....	..
12	Cobalt	1493	CoO	5.68	..	57.5	.....	..
13	Copper	1084	Cu_2O	5.88	1230	43	.....	..
14	Lithium	186	Li_2O	...	1700	143	0.03 –0.033	..
15	Magnesium	650	MgO	3.22	2525	143.5	0.054–0.058	2°
16	Manganese	1207	MnO	4.726	..	91	0.120–0.130	3°
17	Sodium	97.9	Na_2O	2.27	..	100.7	.....	..
18	Nickel	1452	NiO	6.66	..	59	0.13 –0.14	..
19	Phosphorus	44.1	P_2O_5	2.387	..	80	0.045–0.05	2°
20	Silver	960.5	Ag_2O	7.521	..	6.5	.....	..
21	Silicon	1414	SiO_2	2.65	..	94	0.031–0.033	1–2°
22	Titanium	1800	TiO_2	4.26	1560	110	0.052–0.058	3–5°
23	Vanadium	1715	V_2O_5	3.758	658	100	.....	..
24	Zinc	419.4	ZnO	5.65	..	84.8	0.14 –0.157	..
25	Tin	231.9	SnO_2	6.95	1127	68	0.26 –0.29	..

* Under pressure

7. Tin, 8. Manganese, 9. Nickel, 10. Aluminum, 11. Beryllium, 12. Cadmium, 13. Lead, 14. Iron, 15. Chromium, 16. Magnesium, 17. Calcium, 18. Lithium, 19. Barium, 20. Boron, 21. Titanium, 22. Cobalt, 23. Cerium, 24. Antimony, 25. Bismuth, 26. Tellurium, 27. Vanadium, 28. Sodium, 29. Potassium, 30. Carbon, 31. Sulphur, 32. Hydrogen.

1. Oxygen.—Under normal atmospheric conditions, copper is comparatively inert to oxygen. When heated to redness in air, the red cuprous oxide Cu_2O is formed with a thin layer of the black cupric oxide CuO on its outer surface. The cuprous oxide melts at 1235° C. (2255° F.) but a considerable portion is dissolved in the molten copper before this temperature is reached. As the melt freezes, it is thrown out of solution again as solid particles (spheroids) on quick freezing and rod-like forms on slower solidification.[5] There is little, if any, of the cuprous oxide remaining in solution in the solid copper.[4,5,6] However, the cuprous oxide in a concentration of 3.6% (equals 0.4% oxygen) forms with copper an eutectic which melts at 1065° C. (1949° F.). This eutectic is responsible for the hot shortness of oxygen-bearing copper when the latter is heated nearly to its melting point and a tendency to cold shortness when the eutectic has gathered in the grain boundaries and the copper is subjected to cold work. It also has a profound effect on the welding operations as will be brought out later.

Nearly all of the electrical copper wire, cables, bars and shapes (frequently called "electrolytic copper" from one of the refinery processes) as well as the "tough pitch" hot-rolled and cold-rolled copper sheet,* carries from 0.03 to 0.08% oxygen as minute particles of cuprous oxide.

This small amount of cuprous oxide causes no deleterious effect on the electrical properties and only a slight reduction in the ductility of the copper as measured by the elongation and impact test. Thus:

Effect of Oxygen in Rolled and Annealed Copper[7]

Oxygen Per Cent	Tensile Strength psi	Elongation %	Notched Bar Test Ft.-Lbs.	Remarks
0.015	32,370	58.0	49.0	Table averaged and abbreviated from Hanson, Marryat & Ford
0.09	32,950	52.5	45.0	
0.17	34,420	49.2	31.0	

As small as the amount of oxygen is in a good grade of tough pitch copper, 0.02 to 0.08% oxygen, mostly in the form of the cuprous oxide to the extent of possibly 0.17 to 0.71%, it influences strongly the welding properties. This cuprous oxide is relatively bulky as compared to copper. Having a specific density of only 6.0 as compared to 8.9 for copper, it occupies $1^1/_2$ times the volume of equivalent weight of the latter.

The cuprous oxide, if not properly controlled by suitable welding procedure or after treatment, can cause a loss of strength in the weld as high as 60%. This type of weakness was apparently first traced to the cuprous oxide content by M. Le Grix in 1922.[9] It comes about in several different ways:

(*a*) By collecting at the grain boundaries as films of the copper-cuprous oxide eutectic instead of remaining as small spheroids within the grains, the cuprous oxide causes a reduction in both tensile strength and ductility. This occurs when copper has been heated even momentarily to a temperature above the melting point of the eutectic but below the melting point of copper itself. The thin films of copper oxide surrounding the recrystallized grains seem to be more harmful to the ductility of the copper than the rather larger areas of eutectic existing in cast copper. The effect of remelting the eutectic areas in cast copper has not been studied.

The source of heat is immaterial. In any method of fusion welding, there is a zone partially enveloping the weld where the temperature conditions are such as to occasion this type of weakness. The comparison between this over-heated copper with its lowered strength and ductility and the tough-pitch copper is not unlike the comparison between gray cast iron and malleable cast iron. The plates and flakes of graphite in the former allow only moderate strength with little or no ductility while the same amount of graphite in nodular form in the malleable cast iron allows high strength with good ductility.

Le Grix showed that the embrittling of tough-pitch copper during high temperature heating was not caused by oxygen from the outside but rather to the transformation of the harmless spheroids of Cu_2O into the brittle eutectic rejected as larger aggregations to the grain boundaries.

The same author notes that the Cu-Cu_2O eutectic can be again dispersed by suitable hot forging or annealing. Also Leiter[10] demonstrates that the brittle eutectic can be spheroidized by annealing at 900° C. (1652° F.) and cooling slowly until below 850° C. (1562° F.). This treatment leaves the cuprous oxide in the copper but in a less harmful form.

(*b*) In any type of *gas* welding, hydrogen is present to react with the hot cuprous oxide to form water vapor and sponge copper. The action is the same as in the well known "hydrogen embrittlement test" which is often applied to deoxidized or oxygen-free copper wires. Pilling[11] showed that the reducing action of the hot hydrogen is further aggravated by the fact that the steam cannot diffuse outwardly as fast as the hydrogen diffused inwardly. Hence, a high steam pressure is developed in the interstices between the grains at a time when the bond has been seriously weakened by heat. The result is a series of inter-granular fissures which penetrate deeper with increasing time and temperature making the copper very brittle.

Wyman[12] in an extended research shows (*a*) that copper carrying 0.06% oxygen is not embrittled by an exposure of four hours to hydrogen at 400° C. (752° F.), (*b*) the rate of hydrogen attack on oxygen-bearing coppers increases markedly at 700° C. (1292° F.), (*c*) at 800° C. and above, "the damaged structure is literally blown open, the grains being actually separated in many cases, . . . while in the coppers subjected to hydrogen at temperatures below 700° C. (1292° F.), the damaged structure consisted of porous grain boundaries due to the simple reduction of Cu_2O, (*d*) that the elimination of oxygen in copper makes the copper resistant to the action of reducing gases.

Hanson and Marryat [13] and Bassett & Bradley[14] concluded that arsenic is ineffective in protecting heated oxygen-bearing copper from hydrogen embrittlement. Mathewson[8] in a discussion of Wyman's paper remarks that he had previously demonstrated that arsenic did have the effect of ameliorating the hydrogen attack. Smith and Hayward[64] have shown that hydrogen embrittled copper can be restored to its original ductility by hot working in a neutral or reducing atmosphere.

Deoxidized copper or copper free from oxygen is subject to neither of the above hazards.

2. Arsenic.—This element is frequently associated with copper in nature. Its most important application

* Unless made especially for fabrication by welding in which case, it is usually deoxidized.

is its use as an alloying element in fire box copper sheet, tubes and stay bolts used in European locomotives.

Strictly speaking, arsenic is not a deoxidizer for copper[15] though the 0.11 to 0.12% given by Geldbach[4] as the equivalent of 0.045 to 0.050 phosphorus might indicate that it was. The fact that there is such a large production of oxygen-bearing copper carrying from 0.4 to 1.0% arsenic is evidence that the latter has little effect as a deoxidizer.

The insistence of so many of the locomotive designers that fire box copper shall be alloyed with arsenic has its foundation largely in the effect of arsenic (*a*) in raising the annealing or recrystallizing temperature, (*b*) in coarsening the Cu-Cu_2O eutectic of oxygen-bearing copper and thereby making the copper more malleable and (*c*) in increasing the life of the copper in fire box service. As regards point (*b*), the manufacture of the large, heavy sheets demanded for crown, tube and wrapper sheets in the early days of the railroads was by no means easy with the rolling mill and handling equipment of that period. Hence, the copper manufacturers were desirous of using anything which made the copper more workable both hot and cold. Arsenic is effective on this score. Hanson & Marryat[16] found that arsenical copper could be worked hot or cold to any desired extent. Even with 7% arsenic, their laboratory ingots were cold workable to a considerable extent without developing cracks. They found that arsenic was soluble in copper to the extent of 7.25% which amount would stay in solution in the cold, solid copper.

They found further that arsenic has a marked effect in suppressing the evil effect of the Cu-Cu_2O eutectic on the cold workability of cast tough pitch copper. They estimated the optimum arsenic content to be ten or more times the oxygen content. In other words, in the usual tough pitch copper carrying 0.02 to 0.08 oxygen, the arsenic content should be from 0.5 to 1.0%.

The Metallurgist[17] states, "Arsenic in copper is beneficial as it alters distribution of the oxide, causing it to form coagulated lumps instead of the wide-spread eutectic which causes brittleness." Smithells[18] agrees with this view.

Smithells shows that the recrystallization temperature is raised from the 205° C. (401° F.) for pure copper to 250° C. (482° F.) for copper carrying 0.14% arsenic. Larger amounts of arsenic cause a further increase in the recrystallizing temperature though not in direct proportion thereto.

Apparently arsenic is not as effective in raising the recrystallization temperature as is silver or several other elements. Moreover, the effect of arsenic on this score is not additive to that of silver. Gregg[15] states that in a copper carrying 0.029% silver which showed an annealing effect at 350° C. (662° F.) additions of arsenic up to 0.77% had no effect in further raising the annealing temperature as determined by the Rockwell hardness test.

Arsenic, however, is not accepted without challenge as an alloying element for copper. Gregg[15] states that "less than 1% arsenic has little effect on the form or distribution of the oxide . . . . The only effect of arsenic in concentrations below 1% is in modifying the properties of the copper or copper-rich matrix . . . . As much as 0.5% arsenic has little or no effect on the workability or ductility of copper but does increase the tensile strength materially . . . . . while its effect on elongation is inconclusive."

He shows further that arsenic cannot be used for high electrical conductivity copper, the effect of additions being:

Arsenic %	Conductivity %	Remarks
0.05	80	It is probable that the thermal conductivity would be reduced in much the same ratio.
0.25	50	
0.50	38	
1.00	31 to 22	

In an extended review of the literature in Cuivre & Laiton,[19] the editor remarks that arsenic, due to its volatilization, makes the molten copper more or less porous. Hanson & Marryat[13] also found that arsenic up to 1% made difficult the obtaining of sound castings.

However, Geldbach[4] reports Thomas[20] as saying that arsenic did not affect weldability. Geldbach, himself, found that copper welding wires carrying 0.11% arsenic while, producing a lightly viscous flux and requiring boric acid to obtain free flowing, were not adversely affected by the arsenic.

Arsenic oxide floats to the surface and sublimates at the low temperature of 193° C. (397° F.) while the metal itself sublimates slowly at 615° C. (1139° F.). The reviewer, therefore, believes that arsenic in copper offers no obstacle to the making of satisfactory welds. The enormous amount of satisfactory welding that has been done on arsenical fire-box copper since 1921 bears out the correctness of this conclusion.

3. Phosphorus.—Strictly speaking phosphorus is not a metal but rather a wax-like element which is characterized by its extreme avidity for oxygen. It was first used in copper and brass by Parkes of Birmingham in 1849.[21] In molten copper, it first reduces all of the cuprous oxide, the resulting phosphorus oxide vaporizing from the surface as a gas. An excess of phosphorus, over and above that required to completely deoxidize the bath, alloys with the copper making it more fluid when molten and harder when cold. Heyn & Bauer[22] remark that a given amount of phosphorus makes copper harder than the same proportion of tin, They further show that the phosphorus, though only soluble in solid copper to the extent of a possible 0.2% has a markedly lowering effect on the melting point of the resulting alloy until the eutectic is reached with 8.27% phosphorus 91.73% copper which melts at 707° C. (1305° F.). Lindlief[65] places the eutectic at 8.38% phosphorus melting at 714° C. (1317° F.) while Hanson, Archbutt & Ford[24] place the solid solubility of phosphorus in copper as 0.50% at 280° C. This would tend to make a phosphor copper alloy having phosphorus above 0.5% hot short at temperatures above 714° C. (1317° F.).

However, copper with 0.02 to 0.07 phosphorus, besides being thoroughly deoxidized, has greater hot ductility than copper with the same proportion of oxygen. Geldbach[4] remarks, "In small amounts phosphorus raises malleability and toughness of copper. An increase of strength appears at 0.01 to 0.04% phosphorus. The elongation drops as phosphorus is further increased until with over 0.5%, the copper is red short. . . . . As the literature shows, most commercial copper welding wires contain phosphorus." For the same reason, as will be seen later, many manufacturers of copper recommended the use of deoxidized copper sheet for fabrication by welding, designating same as "welding copper."

All of the three phosphorus oxides are vapors at temperatures above 563° C. (1045° F.). Hence, in molten copper, phosphorus will reduce all copper oxide and escape as a gas. The element itself will also escape as a vapor from the molten copper, deoxidizing the film of copper oxide as fast as the latter is formed and thereby keeping a clean, bright surface on the molten weld

pool which is agitated gently by the escaping vapor. This reaction is sometimes referred to as the self-fluxing effect of phosphorized copper.

Phosphorus has the most sharply depressing effect on the conductivity, electrical and thermal, of any of the elements usually associated with copper. Smith[23] gives the values of Table 2. The thermal conductivities have been converted to per cent of that of copper which was measured as 0.941 cal./sq. cm./cm./sec./° C. at 20° C.

Table 2—Electrical and Thermal Conductivities of Copper-Phosphor Alloys. (after Smith)

Analysis, %		Electrical Conductivity, % I.A.C.S.	Thermal Conductivity 20° C., %
Phos.	Copper	20° C.	of Copper
0.000	100.	101.66	100.
0.042	Rem.	74.72	77.3
0.075	"	56.32	60.4
0.239	"	31.77	36.8
0.480	"	20.42	23.0
0.677	"	14.85	17.5
0.930	"	11.29	13.7

Phosphorus also has a decided effect in raising the recrystallization temperature of copper. Hanson, Archbutt and Ford[24] show an annealing temperature of approximately 325° C. for copper carrying 0.10% phosphorus which is an increase of approximately 125° C. over that required for pure copper.

All things considered, there is ample reason why phosphorus is used almost universally as the one deoxidizer for copper welding sheets and rods. On the other hand, it is by no means certain that phosphorus will always be preferred as a deoxidizer for welding copper. Several other elements offer promise in this respect.

4. Silver.—Like arsenic, silver is frequently associated in nature with copper. The average metal reduced from the copper ores mined in the United States in 1929 was 1.41% copper and 0.0009% silver per ton of ore.[2] Practically all of the silver is removed from the copper in the electrolytic refining process. Silver, melting at 960.5° C. (1751° F.) with the high boiling point of 1950° C. (3542° F.) is readily soluble in molten copper, lending fluidity to the bath, but is soluble in annealed solid copper to an extent of less than 0.4%.[25] An alloy of 72% silver and 28% copper[15] forms an eutectic melting at 780° C. (1436° F.). Additions of other elements, as zinc or cadmium, are necessary to further lower the melting point.

Silver in amounts of only 0.10% in copper raises the annealing temperature from 185 to 325° C.[15]

It has the least depressing effect on the electrical and thermal conductivity of copper of any of the elements commonly associated therewith. Smithells[18] shows in graph form that silver up to 4% does not greatly increase the electrical resistivity (lower the electrical conductivity). It is clear, therefore, that silver does not affect the thermal conductivity as badly as arsenic or phosphorus. He further shows that the grain size of copper is reduced as much as 50% with 0.10% silver. Kenny and Craig[26] show (*a*) that the annealing temperature of copper increases rapidly with additions of silver until the silver content reached ten ounces per ton (0.034%) and then more slowly with further additions and (*b*) that forty ounces of silver per ton of copper (0.14%) has no adverse effect on the mechanical properties.

The silver oxide dissociating at 300° C. (572° F.) does not interfere with welding operations or affect the copper weld metal. Silver cannot, therefore, deoxidize molten copper. Geldbach,[4] however, found that by coating electrolytic copper welding rods with silver, he obtained protection of the metal in the heating-up stage and a clearer weld pool than could be noted with the bare wire.

With all its beneficial effects on copper exceeding those of arsenic for the most part, the reviewer believes that silver will probably not be used greatly in the copper base metal for welding on account of the expense. However, the beneficial results obtained with an amount as small as thirty ounces per ton (0.103%) will sometimes be justified. This is particularly true with such highly specialized products as welding wire.

5. Silicon.—This element of relatively low specific density of 2.4 and melting at 1420° C. (2588° F.) is a powerful deoxidizer for copper as well as a useful alloy element. It will not vaporize as the element, having the high boiling point of 2600° C. (4712° F.). Nor will its oxide vaporize in welding operations, since its boiling point exceeds 2230° C. (4046° F.). The pure oxide is rather refractory melting at 1670° C. (3038° F.) or above.

However, the silicon oxide reacts instantly at the fusion temperature of copper with the oxides of copper, manganese, zinc, iron, tin, calcium, sodium and boron (the latter three from the fluxes) to form an amorphous glass melting at a temperature nearly coincident with that of the copper or copper alloy. This reaction takes place at the surface of the molten pool, the silicon oxide and the resulting glass having a rather low specific density of the order of 2.75. The film of molten glass spreads uniformly over the pool of metal completely covering it. This obtains with additions of silicon as low as 0.03%. The glass envelope freezes within a few degrees of the metal it encloses. It has a different coefficient of contraction from that of the metal so that, unless extremely thin, a good portion of it cracks and flies off.

With copper alone, the silicon glass film is rather refractory and may give some trouble on the weld pool. This difficulty is avoided when manganese or zinc is present from the metal, or sodium from the flux, to lower the melting point of the film.

Smith[2] shows a solid solubility of approximately 4% silicon in copper. With silicon in increasing proportions up to 4%, there is a marked increase in strength with excellent ductility both hot and cold. With the small silicon content, 0.03% to 0.07%, necessary to insure complete deoxidation, the copper has better hot and cold ductility than tough pitch copper.[27] With larger proportions of silicon, the hot forgeability is excellent up to a bright red heat estimated at 800° C. (1472° F.). At temperatures nearer the liquidus, the copper silicon alloys have a hot short range which must be given consideration when welding or forging such alloys as Everdur.

Silicon has a sharply depressing effect on the electrical and thermal conductivity. One quarter per cent of silicon in copper results in an electrical conductivity of approximately 37% I.A.C.S. with a correspondingly depressed thermal conductivity.

Silicon improves the copper alloy sheets for resistance spot and seam welding.[28] This is due in part to its lowering effect on the electrical and thermal conductivity and in part to the fact that the oxides formed are metallurgically inert, flux-like films. Phosphorus, on the other hand, in a copper alloy, volatilizes in the heat of the resistance weld and tends to form a bond between the electrode and the surface of the sheet. This means frequent resurfacing of the electrodes. Silicon, therefore, is to be preferred for copper alloy sheet which is intended for resistance spot or seam welding.

6. Zinc.—This element gives us the most extensive and useful series of the binary alloys of copper. In amounts of 1% and less, however, it is of comparatively

little use as a deoxidizer and its effect on the properties of the copper is slight.

Pure zinc melts at 419.4° C. (787° F.) and boils at 907° C. (1665° F.). It begins to vaporize at about 650° C. (1202° F.). The oxide is rather heavy and refractory having a specific density of 5.6 and lying inert until it sublimates at 1800° C. (3272° F.). Ordinarily, however, the oxide does not interfere with welding operations as the zinc vapor burns to zinc oxide some distance above the weld pool, the fine particles of oxide floating off in the air—the familiar white zinc oxide smoke.

Gerbeaux[29] noted that if the oxyacetylene torch is made strongly oxidizing, some of the refractory oxide can be made to lie on the weld pool acting as a blanket to suppress in part further vaporization of the zinc.

Zinc is readily soluble in solid copper as an alpha constituent up to approximately 39%. Bauer and Arndt[30] studying the diffusion rates of zinc into oxygen-bearing copper at various temperatures noted a rapid diffusion of the zinc into the solid copper after the melting point of zinc was reached. The zinc reduced the cuprous oxide, the resulting zinc oxide being deposited as loose layers in the outlines of the original cuprous oxide.

In amounts of 1% or less, it does not exert any serious adverse effect on the thermal or physical properties. It is not used in copper welding rods. Geldbach remarks, "In general practice, we do not use zinc-containing copper (welding) wire on account of its sensitivity to overheating which is especially bad on welding thicker metal." He probably refers to the vaporization of the zinc which takes place rapidly at the copper fusion temperature.

7. Tin.—Tin is characterized by an unusually low melting point 232° C. (450° F.) with an unusually high boiling point of 2270° C. (4118° F.). It oxidizes preferentially to copper but is not effective as a deoxidizer. This is partly due to the relative high specific density of 6.95 and partly to the clinging nature of the tin oxide which as Geldbach[4] shows stays in the melt. It may be, however, as films of the solid oxide at temperatures lower than 1127° C. rather than as films of the melted oxide as Geldbach supposes since the solid SnO_2 decomposes[3] at 1127° C. (2060° F.). Tin, therefore, should not be used alone with copper. With phosphorus, however, which reduces the tin oxide as effectively as it does that of copper, tin alloys with the copper improving its strength, hardness and wear resistance with a slight reduction in its cold ductility. With tin less than 1%, there is no appreciable loss of hot ductility.

In ordinary casting only about 5% of tin remains in solid solution in the copper[2] though the equilibrium diagram by Stockdale[31] shows a possibility of retaining 16% in solid solution. Bauer and Arndt[30] show that tin reduces the cuprous oxide but more slowly than zinc.

Tin lowers the melting point of the resulting alloy but with a rather long temperature range between the solidus and liquidus. This explains the hot shortness of the tin bronzes in the temperature range just below their fusion temperature. It also explains the dendritic structure and shrinkage fissures which are sometimes noted in slowly-cooled high-tin bronzes.

8. Manganese.—This element is as useful in the nonferrous alloys as it is in the ferrous alloys. Its principal rôle is that of a deoxidizer and desulphurizer. The pure metal melts at 1260° C. and boils at 1900° C. (3452° F.). The dioxide MnO_2 gives up part of its oxygen at 535° C. (995° F.), Mn_2O_3 yields oxygen at 1080° C. (1976° F.) while the manganous oxide MnO is stable until it melts at 1650° C. (3002° F.). The manganese oxides float as islands of the oxide on the molten copper.

Manganese dissolves readily in copper[2] with a lowering of the melting point and a hardening effect, until with 34.5% manganese in copper the alloy is very brittle and melts at 868° C. Manganese depresses the electrical and thermal conductivity though not as rapidly as does phosphorus, silicon or arsenic.

Smithells[18] shows that 0.23% of manganese in copper will raise the recrystallizing temperature from the 205° C. (401° F.) for pure copper to 320° C. (608° F.). Under similar conditions, 0.24% silver will raise this point to 350° C. (644° F.), 0.24% of tin to 375° C. (707° F.) and 0.36% phosphorus to 325° C. (617° F.).

Geldbach[4] quoting Ledebur-Bauer and Hollard concludes that manganese makes the copper viscous and forms films of oxide which lower the quality of the weld. In his own work, he had no difficulty in fluxing the surface of the weld pool made by melting copper wires carrying 0.05% manganese and 0.13% manganese, respectively.

Thomas[32] made oxyacetylene welds in commercial copper, using a copper welding rod analyzing 0.132 manganese and 0.045 phosphorus, which proved slightly superior to welds made with electrolytic copper wire of 0.075% oxygen and slightly inferior to a welding rod which analyzed 0.928% silver, 0.041% phosphor and 0.015% oxygen, remainder copper.

On the whole, it appears that while manganese may be used as a deoxidizer and desulphurizer for copper and can be fluxed rather easily, phosphorus, silicon and several other elements are more active as deoxidizers and magnesium is a more active desulphurizer. Hence, it is probable that manganese finds its appropriate place as a third element with silicon, nickel, etc.

9. Nickel.—Melting at 1452° C. (2646° F.) and boiling at 2900° C. (5252° F.) with a specific density almost identical to that of copper, 8.9, nickel forms a solid solution with copper in all proportions raising the melting point continuously with increasing proportions of nickel. It is not a deoxidizer to copper but needs a deoxidizer and desulphurizer itself. It is more important as a major constituent of a copper alloy than as a small addition to copper.

The nickel oxide is quite refractory, closely clinging and difficult to dissolve with the ordinary fluxes. The NiO oxide is also quite heavy, having a specific density of 7.45. It is prone, therefore, to give trouble in welding operations.

However, with only 0.10% nickel in copper, Geldbach[4] found that the welding rod gave an oxide film which was rather thin and easily fluxed in boric acid and also in sodium fluoride, obtaining therewith sound welds. He remarks further that the nickel increases the resistance of the copper to high temperatures and improves the strength. He is of the opinion that the copper welding wire with a small addition of nickel is poorly adapted for welding thin copper plates (probably on account of its high melting point) but is quite serviceable for welding thick plates.

Smithells[18] shows that 0.28% nickel in copper raises the recrystallizing temperature 45° C. being less effective than arsenic, phosphorus, silver, tin, manganese or silicon but more effective than zinc or iron.

Summarizing the effect of nickel, it looks to the reviewer that small additions of nickel alone in copper will not make a satisfactory welding rod but will require the use of a third element to lower the melting point, deoxidize, desulphurize and improve the flowing properties.

10. Aluminum.—Small additions of this element are readily soluble in copper with strong deoxidizing effect and comparatively little lowering of the melting point. Seven and one-half per cent of aluminum[2] in copper shows a melting point of 1031° C. (1888° F.).

The aluminum oxide makes a thin, continuous, solid,

refractory film over the molten pool. It has a comparatively low specific density of approximately 4% and, therefore, floats on the weld pool. However, it is a solid, tough web, melting at 2050° C. (3722° F.) and is apt to become folded into the flowing metal.

Geldbach[4] tried copper welding wires with additions of 0.02 to 0.03% aluminum and also with combined additions of 0.01% aluminum with 0.04 to 0.05 phosphorus. He remarks, "With a copper welding rod carrying 0.02 to 0.03 aluminum, an exceedingly tough and viscous film occurred, when welding with these wires in the absence of a flux. Solid films of oxide formed on the surface of the melt which made flowing together difficult and which had to be pushed away with the rod. These films remained on prolonged application of the flame. . . . With a fluxing agent made up of boric acid with 20% cryolite, the behavior of the weld metal improves, approaching that of the phosphor copper welding rod. . . . The welding wires having 0.01 aluminum with 0.04 to 0.05 phosphorus additions behaved like the wires having aluminum alone."

Smithells[18] reports a lowering of the recrystallizing point of copper from 205 to 150° C. as a result of adding 0.12% aluminum. This is a contrary effect to that of most of the metals associated with copper.

Aluminum added to copper deoxidizes it and, unlike most additions, has no ill effect on the hot workability of the resulting alloy. It depresses the electrical and thermal conductivity of copper more rapidly than nickel or tin but less rapidly than phosphorus, silicon or manganese.

The reviewer believes that the disadvantages of the aluminum addition to copper outweighs the advantages when the alloy is to be used as a gas welding wire.

11. Beryllium.—Pure beryllium is one of the extremely light metals having a specific density of only 1.85. It melts at 1350° C. (2462° F.) and boils at 1530° C. (2786° F.). It has a strong affinity for oxygen acting powerfully as a deoxidizer for copper. The oxide is extremely refractory, melting at 2570° C. (4658° F.), and difficult to dissolve with a flux.

The beryllium copper alloy develops upon melting a thin, continuous, tough, solid membrane similar to the film of aluminum oxide of the aluminum-copper alloy but, if anything, tougher and more resistant to fluxing.

Beryllium is soluble in copper to the extent of 2%, more or less, to form an alpha solid solution. It has a sharply reducing effect on the melting point of the copper-beryllium alloy which is approximately 955° C. (1751° F.) with 2¼% beryllium with incipient fusion beginning at 864° C. (1587° F.).

The alloy has excellent hot forgeability at a dull red heat and is also cold ductile in the annealed state. It is hot, short at temperatures above 864° C. (1587° F.).

Geldbach[4] investigated additions of 0.01 to 0.45% beryllium in copper and found the type of film forming on the molten pool, thickest with the wire containing the highest beryllium content. With the wire having the lower beryllium content, he was able to dissolve the film with a flux of boric acid plus 20% sodium fluoride and obtain free flowing of the weld metal. Polished sections showed sound metal free from entangled oxide.

He also tried copper wires carrying 0.04 beryllium with 0.50 chromium and found that the skin of oxide on the weld metal was so viscous and tough that fluxing agents had scarcely any effect.

12. Cadmium.—Cadmium copper is a well known alloy used for trolley and transmission wires having good strength and electrical conductivity. Only about 2% of cadmium is soluble in copper to make an alpha alloy.

The pure metal has a very low melting point of 321° C. (610° F.) and exerts a lowering effect on its alloys with copper. It vaporizes at 767° C. (1413° F.) which is lower than the boiling point of zinc.

The oxide is heavy, having a specific density of 6.95 to 8.15. It has a high melting point, above 1426° C. (2599° F.) but is unstable above 900° C. (1652° F.). Matteoli[32] shows that starting with 0.55 to 0.93% of cadmium in the welding rod, only 0.20 to 0.40% remains in the weld after fusion with the oxyacetylene torch, i.e., about 38% of the cadmium in the welding rod is lost in making a weld.

Geldbach[4] found that "copper wires alloyed with 0.2% and 0.3% of cadmium gave the most viscous surface layers of all wires tested except the beryllium and chromium alloys. The layer was thicker and more viscous as the cadmium addition was increased. . . . . This skin dissolved only with difficulty and incompletely on using boric acid and sodium fluoride and is formed immediately as soon as the use of the fluxing agent is discontinued or interrupted. We may say that cadmium is very poorly adapted as an addition element to copper welding wires."

In 1934, Matteoli,[33] having read Geldbach's treatise, made welding tests on electrolytic copper with cadmium containing wires and the oxyacetylene torch with results as given in Table 3.

Judging from the results in the table, the reviewer is of the opinion that cadmium copper with a trace of phosphorus is an improvement on the use of an electrolytic copper welding wire. The use of 0.08 to 0.10% phosphorus with the cadmium in the copper welding wires is a still greater improvement but is not appreciably superior to 0.05 to 0.10 phosphorous copper welding rods sans cadmium.

Matteoli's own findings are stated thus:

1. "The fluidity of a welding rod of copper alloyed with phosphorus, and with cadmium and phosphorus, is much greater than electrolytic copper or copper alloyed with cadmium but without phosphorus.

2. "Good results have, however, been obtained with copper-cadmium rods in connection with fluxes commonly used for copper welding.

3. "Using alloys of copper, cadmium and phosphorus,

Table 3—Results of Mechanical Tests on Copper Welds. (after Matteoli)

Material	Chemical Composition Cd. %	P.	Special Elements	Ultimate Strength Lbs. per Sq. In.	Elongation %	Remarks
A6. Cu Electrolytic	..	..	..	30,000	22.0	Average of 5 tests
B7. Cu-Cd Alloy	0.30	trace	..	30,000	17.8	Average of 5 tests
C8. Cu-Cd Alloy	0.90	trace	..	31,000	25.0	Average of 5 tests
D9. Cu-Cd Alloy	0.80	trace	0.2	32,000	27.1	Average of 5 tests
E4. Cu-P Alloy	..	0.05	..	32,000	29.2	Average of 5 tests
F2. Cu-P Alloy	..	0.10	..	32,000	29.1	Average of 5 tests
I5. Cu-Cd-P Alloy	0.80	0.08	..	32,000	29.9	Average of 5 tests
L3. Cu-Cd-P Alloy	0.50	0.10	..	33,000	31.0	Average of 5 tests

welds have been made which after cold hammering and annealing have been tested to 31,000–33,000 pounds per square inch and 37% elongation.

4. "Although examination of the metal in proximity to the weld shows no sign of burning, it has not been possible to avoid an increase in the size of the granular structure of the metal. Fracture generally occurs near the weld.

5. "It must be borne in mind, however, that the results obtained are more dependent on the skill of the operator than they are upon the characteristics of the welding rod used.

"Further practical tests are being carried out to determine the respective merits of other copper welding rods in order to be able to judge the practical use of Cu-Cd Alloy rods."

With 1% or less of cadmium in copper deoxidized with a trace of phosphorus, we obtain a metal with good cold ductility and good forgeability at dull red heat as well. Forged at a bright red heat, it is difficult to avoid hot short cracks. Smithells[18] remarks that while "pure copper can be softened by an anneal of $\frac{1}{2}$ hour at 260° C., Copper with 1% cadmium is unaffected by this treatment."

While it is clear that good welds can be made with cadmium copper, it is the reviewer's thought that one should seek an element that (*a*) is less volatile, (*b*) has a lighter and more soluble oxide and (*c*) has better hot ductility.

13. Lead.—Lead melts at 327° C. (621° F.) and boils at 1613° C. (2935° F.). It is insoluble in copper, existing in solid copper as nearly pure lead. It will, therefore, begin to sweat out at 327° C.[18] Also, as is well known, lead makes copper red short. Geldbach[4] reports that "copper wires having 0.10% lead have a tendency to brittleness during forging." He reports further:—"Welding tests showed that solid films appear on the surface of the melt. This makes the flux sluggish although under this skin is a watery melt. Probably this skin is something other than an oxide. . . . complicated compounds whose compositions have not been studied in detail as only the welding behavior is of interest." Hence, lead may be dismissed from further consideration as an element for copper welding rods. They should be kept scrupulously free of lead.

14. Iron.—Hanson and Ford[34] show that iron is soluble in copper to an extent of about 0.2%. They also report that in amounts of 1% or less, it has no appreciable embrittling effect on copper. Owing to the low solubility of iron in the copper, the effect of iron on the properties of the copper depends a great deal upon the thermal treatment of the alloy.

The same authors show that "iron acts mildly as a deoxidizer for copper but creates films that destroy the continuity of the molten metals." Hanson and Ford found that 0.06% of iron in copper has little effect on the annealing point but 0.73% iron raises it about 50° C. while Smithells reports that 0.20% of iron depresses the annealing point about 15° C.

Hanson and Ford[34] show the following effect of iron on copper, hot rolled and annealed at 700° C.

Analyses, % Oxygen	Iron	Tensile Strength psi	Elongation, %	Electrical Conductivity I.A.C.S. %
0.014	0.06	32,500	57.1	101.0
0.003	0.20	31,400	60.0	54.1
0.004	0.40	33,600	59.9	40.8
0.008	0.73	37,600	51.7	42.0
0.005	0.96	35,800	45.0	38.9

Geldbach[4] found that with additions of 0.10% ferro-uranium and 0.10% titanium to copper welding wires and also 0.10% ferro-vanadium to the welding wires, he obtained bad films which interfered with welding and were difficult to flux.

On the whole, the reviewer concludes that iron is undesirable in a copper welding rod.

15. Chromium.—This element which melts at 1615° C. (2939° F.) is only slightly soluble in copper and forms on molten copper a tough, refractory film which is probably the solid oxide melting at 1990° C. (3614° F.). Geldbach[4] found that with copper containing about 0.13% chromium, he obtained "extremely viscous and thick surface layers which were only slightly dissolved with boric acid, sodium fluoride or sodium carbonate." He obtained similar results with rods containing 0.04% beryllium plus 0.5% chromium and 0.05% titanium plus 0.5% chromium, the viscosity and thickness of the refractory film being greater than with the 0.13% chromium above.

Chromium copper is amenable to precipitation hardening, the electrical conductivity being at the same time improved. This fact makes the alloy suitable for resistance spot welding electrodes.

It appears at this time that except for the use mentioned above, the chromium copper has little application in copper alloy welding.

16. Magnesium.—This element is a powerful deoxidizer for copper the resulting, solid, refractory magnesium oxide being dispersed as small particles. Bauer and Arndt show that the diffusion of magnesium through copper is very rapid. Faster than is that of tin in copper.

Geldbach[4] found with 0.06% magnesium that very viscous surface films were formed with occasional spattering when no fluxing agent was used. Using boric acid with 20% sodium chloride as a flux, he was able to dissolve the film with an ebullition on the surface which he takes to be $MgCl_2$ (melting point of 712° C.) boiling off. This made a surface porosity though the underneath weld metal was sound. He remarks that this rod would be unsuited for vertical or overhead welding.

Smithells[18] remarks that magnesium is used as a deoxidizer and desulphurizer in copper-nickel alloys having been discovered valuable in this capacity by Fleitman in 1879.

17. Calcium.—Zickrick[35] shows that copper is effectively deoxidized with 0.0375% calcium the latter appearing as very small specks throughout the metal. He notes that "with calcium 0.15% or more considerable care had to be exercised in order to avoid hot forging cracks."

Geldbach[4] remarks that "calcium behaves like sodium, vaporizes, but less rapidly." He made up a copper wire carrying about 0.20% of $CaSi_2$. It behaved like wires containing only silicon.

Judging by such data as its convenient melting point 810° C. (1490° F.), its moderately high boiling point 1439° C. (2622° F.) and the well known good slagging qualities of its oxide (in spite of its high melting point of 2572° C. (4662° F.)), the reviewer suggests that calcium should be studied further particularly as the deoxidizer in a ternary alloy.

18. Lithium.—This is the lightest of the metallic elements with which we have to deal. It has a specific density of only 0.534. It also has a very low melting point at 186° C. (367° F.) though it has a relatively high boiling point at 1336° C. (2437° F.). The oxide is rather refractory.

Geldbach[4] remarks that there is little published information on the welding properties of lithium copper. W. Kroll patented in Germany in 1928 (D.R.P. #458237) the use of lithium in copper in amounts of 0.01 to 0.30%

claiming that it promoted fluidity of the copper and removed sulphur and oxygen. Geldbach made copper welding wires carrying 0.07% and 0.10% lithium which gave a clear, free flowing melt without the use of flux.

He remarks, "Probably on account of the great affinity of lithium for sulphur, oxygen and other gases, complex compounds or eutectics of lithium with those gases are formed which are fluid at welding temperatures, that the oxide layer is not pure lithium oxide may be judged from the melting point of lithium oxide (over 1700° C.). A high fluidity was attained (similar to high-phosphorous wires) by using boric acid as fluxing agent."

Matteoli[33] showed that 0.08 to 0.13 lithium added leaving 0.03 to 0.04% in the copper effectively deoxidized the metal as it withstood one hour at 800° C. in hydrogen with no ill effects. Lithium was also studied by Burkhardt & Sachs[66] who obtained some promising results. On the whole lithium appears to have some useful and valuable properties.

19. Barium.—Like lithium, there is a dearth of information on barium in copper. E. L. Munson patented (U. S. 1,966,260, July 10, 1934) the use of barium in copper alloy welding rods in amounts from 0.001 to 5.0%. He claims the effect of barium is to deoxidize the melt and promote soundness and ductility in the weld metal, thus preventing the formation of shrinkage cracks. Matteoli[33] showed that 0.10 to 0.17% of barium added leaving 0.06 to 0.08% in the copper thoroughly deoxidized the copper as it withstood one hour at 800° C. in hydrogen with no ill effects. He showed similar results with strontium.

20. Boron.—This element is only soluble in copper to a limited extent, if at all. Geldbach[4] tried to alloy copper with boron and with boron suboxide wrapped in copper sheet and rapidly plunged into a melt of copper—the boron burned out with a light green flame. He remarks, "I believe it probable that boron containing welding rods for copper, its manufacture being possible, would have good fluidity and affect the weldability favorably."

Weintraub[36] used the boron suboxide and boron carbide to deoxidize copper, the former being preferred. 0.75% is used. The boric anhydride formed floats to the top and carries metallic oxides in solution.

Andrieux[37] gives an excellent review of the scanty literature on the subject describing in detail the manufacture of nickel-boron alloys by electrolysis at 1100° C. in a graphite crucible. The bath is fused magnesium or calcium borate and fluoride. The crucible is the anode and nickel the cathode. However, as the latter is enriched in boron, it melts off and drops to the bottom of the crucible. He apparently did not make boron copper but suggested the manufacture of a ternary alloy in which we should have copper deoxidized with the 12% boron-nickel master alloy.

The reviewer believes, with Geldbach, that, owing to the beneficial fluxing effect of the boron oxide, boron would be a desirable deoxidizer for copper. It seems to be only slightly soluble in copper but apparently no one has measured exact proportions. Clearly, it is soluble in nickel to a considerable extent.

21. Titanium.—Kroll[38] shows the copper equilibrium diagram for alloys up to 25% titanium and 75% copper. The system belongs to the age hardening type, the solid solubility being 3 to 4% at 900° C. and almost nil at 20° C. Kroll prepared his alloys under pure argon in an alundum crucible and found it necessary to heat treat them under hydrogen on account of the avidity of titanium for oxygen when heated in air.

Guertler[39] reports titanium as having a strong affinity for nitrogen as well as oxygen. Geldbach[4] prepared wires having 0.03 to 0.10% titanium which gave a normal, clear surface to the weld pool with those having 0.06% or less titanium and a thin, solid surface film with the one having 0.10% titanium. This film was easily fluxed with boric acid giving a free flowing metal.

He reported further, "The relatively large solidification range was remarkable. This behavior agrees with Kroll's diagram which shows a steeper drop of the solidus than in most binary copper alloy diagrams. Consequently, the mobility of titanium-copper welding wire is relatively high and these rods are good for vertical and overhead welding."

The reviewer suggests that we should investigate the stability of the titanium copper at elevated temperatures in air noting its strength, ductility and scaling properties before we can accept Geldbach's conclusion.

22. Cobalt.—Smithells[18] shows that cobalt is soluble in both copper and brass in appreciable amounts. Geldbach[4] dismisses cobalt without an actual trial on account of its reputation of causing an increase in the melting point of copper and of developing films partly within the copper melt and partly on the surface.

However, tin and cadmium also develop such films and yet form useful alloys when deoxidized with phosphorus. Cobalt may behave in a like manner. Andrieux[37] shows that cobalt can be alloyed with boron up to approximately 12.0% boron by high temperature, electrolytic methods. The reviewer suggests that this master alloy may yield a satisfactory ternary alloy in copper.

23. Cerium.—Geldbach[4] also dismissed cerium on account (*a*) of the high melting point of the oxide, 1950° C., (*b*) its being slagged with great difficulty and (*c*) its making a brittle alloy with copper. It appears to be a strong deoxidizer for copper.

24. Antimony.—Geldbach[4] shows that copper wires with antimony can be fluxed to secure good flowing. Smithells[18] reports that Wieland[40] found this element "forms brittle intercrystalline films and if present in excess of 0.005%, makes the alloy unworkable." Amedeo[21] reports antimony as having an embrittling effect on copper rendering it difficult to work hot or cold.

Archbutt and Prytherch[43] made perhaps the most searching investigation thus far completed on the effect of antimony in copper. They found 10% of antimony soluble in annealed copper. It has a sharply reducing action on the melting point, 1%, reducing the solidus temperature nearly 60° C. and a greater effect in raising the annealing temperature than the same proportion of arsenic. It lowers the electrical conductivity but only half as much as the same proportion of arsenic. In amounts of 0.22%, it has a pronounced effect in raising the tensile strength without loss of ductility. Antimonial copper, with antimony up to 0.47%, can be hot rolled satisfactorily. It can be cold rolled with antimony up to 0.85% though with this amount the metal is hot short. They had some difficulty in obtaining sound antimony, tough-pitch copper castings by the usual commercial methods, resorting finally to melting in vacuo.

The same authors studied the combined effect of arsenic and antimony in tough-pitch copper finding (*a*) that arsenic helps in the hot rolling of antimony copper, (*b*) that antimony improves the strength of arsenical copper and (*c*) that the two elements together are more effective in raising the annealing point than either element alone.

They did not study the effect of antimony on deoxidized copper.

It appears from the work of Archbutt and Prytherch that antimony should be given further consideration as a third alloying element in copper welding rod. This is

particularly true as the melting points of the metal and the oxide fall within convenient limits, 630° C. (1166° F.) and 656° C. (1211° F.), respectively, while, with a boiling point of 1380° C. (2516° F.), there should be no serious ebullition at the fusion temperatures of copper. The depressing effect of antimony on the solidus temperature may aid in vertical and overhead welding, if it does not make the metal too hot short.

25. Bismuth.—Smithells and others report bismuth as being objectionable in copper in excess of 0.005% on account of its bad effect on the workability of the alloy. Hanson and Ford[41] found the solid solubility of bismuth in copper less than 0.002%. Even this small amount had an adverse effect on the properties.

26. Tellurium.—Objectionable for the same reason as bismuth.

27. Vanadium.—Very little information is available in the literature on the effect of vanadium on copper. Geldbach[4] tried additions of ferro-vanadium to copper obtaining therewith a very viscous surface film but was not able to evaluate therewith the effect of vanadium alone.

28. Sodium.—Geldbach[4] quotes Mathewson to the effect that sodium is insoluble in copper. So much so that it could not be detected spectroscopically.

29. Potassium.—Same behavior as sodium.[4]

30. Carbon.—Insoluble in copper. This fact is of moment as thereby we may weld with the carbon arc without fear of the weld pools being contaminated with elementary carbon.

31. Sulphur.—Well known to be detrimental to copper and nickel alloys. Affects the workability and soundness of the weld metal. If, for any reason, it is present in the metal or torch flame, a desulphurizing element such as magnesium, manganese, lithium, zinc or aluminum should be used.

32. Hydrogen.—Hydrogen exerts a strongly deoxidizing effect on molten copper. Thus, copper melted by the use of the oxyhydrogen torch, the oxyacetylene torch or in a hydrogen atmosphere furnace will be deoxidized except for the oxygen pick-up in the freezing period (if solidified in air).

In the case of solid oxygen-bearing copper exposed to hydrogen for a short time at temperatures above 700° C. or for a longer period at lower temperatures,[12] the deoxidizing action extends inwardly with time reducing the cuprous oxide inclusions and, if the temperature be high enough, disrupting the grains making the copper very weak—"hydrogen embrittlement."

In molten, pure copper deoxidized with hydrogen, a certain amount of the hydrogen is dissolved which is thrown out of solution as the copper freezes. However, since we have pure copper, the solidification is rapid particularly on the surface. Hence, the rejected hydrogen is entrapped in the solid copper making it very porous.

The remedy for the first ill effect is to use a deoxidized copper with a slight excess of the deoxidizing element left in the melt. The remedy for the second ill effect is to use an element (which may be the same as the deoxidizer or a third element) which will (*a*) diminish the solubility of copper for hydrogen and (*b*) spread the temperature range between liquidus and solidus so as to give the rejected gas a better opportunity to escape.

Smithells[18] shows in diagram form the solubility of hydrogen in copper at various temperatures. There is a marked, sudden increase as the copper goes from the solid to the liquid state. There is prone to be a sudden decrease at the freezing point with decreasing temperatures. He also shows in a graph, after Sieverts,[42] that tin and aluminum diminish the capacity of copper for dissolving hydrogen, silver being neutral and nickel sharply increasing it.

PART 2

Copper Welding

General

In this section are presented notes from the literature on copper welding in sufficient detail as to make it possible to apply the information to actual welding problems. The base metal in every case is tough-pitch, oxygen-bearing copper or deoxidized copper. All of this material has a high thermal conductivity and methods of overcoming this obstacle must be an integral part of any welding procedure. A review of the foregoing notes on oxygen, phosphorus, arsenic, silver, silicon, manganese and hydrogen will help in an understanding of the processes outlined below.

Tough-Pitch Copper, Electrolytic Copper, Arsenical Copper, Deoxidized Copper, "Welding Copper."—By "tough-pitch copper" is usually meant Lake copper or copper cathodes melted in a reverberatory furnace and partially deoxidized with green wooden poles until, when poured into an open mold, the solidified top of the copper is nearly flat.

Previous to melting in this furnace, the cathodes are obtained by electro-deposition from an impure copper anode. The impurities are left in the bath as the copper is transferred through the acid electrolyte, from anode to cathode. At "tough-pitch," the electrolytic copper is nearly 99.2% pure copper, including a small fractional per cent of silver, the remaining being 0.20 to 0.70 per cent cuprous oxide.

In this discussion the terms "tough-pitch" copper and "electrolytic" copper indicate an oxygen-bearing copper. The "arsenical" copper is quite similar except that 0.4 to 1.0% of copper has been displaced with an equivalent amount of arsenic.

"Deoxidized copper" is as a rule treated with phosphor copper or other deoxidizer as it comes from the poling furnace or it is simply "tough-pitch" copper remelted and treated with a deoxidizer. It is generally 99.9% pure copper plus 0.02 to 0.07 phosphorus. Arsenical copper is generally oxygen-bearing copper, though some deoxidized arsenical copper has been made and used.

Deoxidizers, other than phosphorus, may be used but have not generally been made available to the trade. Miller[27] expressed a strong preference for silicon as a deoxidizer.

"Oxygen-free high-conductivity copper," an exceedingly pure copper, has been in use for some time for electrical wire but has not been commercialized to any extent in large sheets for welding.

The physical properties of copper are given in handbooks,[2,3] etc., and need not be repeated here. It should, however, be pointed out that copper has no plastic temperature range during which it may be hammer-welded. It melts in air at a temperature slightly below 1083° C. (1981° F.) to a water-thin liquid. In the temperature range just before it melts, it cannot be hammered on account of its hot shortness due to the cuprous oxide eutectic or that of the deoxidizer.

Apparently little was published on copper welding prior to 1912 when Carnevali[44] lectured to the Institute of Metals on copper welding. The greater part of the atten-

tion of the investigators up to 1930 has been directed toward the welding of locomotive fire-boxes which in European, Asiatic and Australian countries have for many years been made of tough-pitch, arsenical copper. Most of these copper fire-boxes have been riveted, but attempts to repair them by welding were made in 1919 or earlier, and all-welded fire-boxes were first made for the German Railways at Magdeburg-Buckan in June 1923[45] and about the same time by Samesreuther & Co. in Butzbach.[46]

In this country, the copper fire-box was non-existent and only a comparatively small amount of copper was welded until the revival of the brewing industry in 1933 and the distilling industry in 1934 brought copper welding sharply into focus. Before that, most of the coppersmiths joined copper by riveting and soft soldering, brazing and silver soldering. The fusion welding of copper alloys such as Everdur, starting in 1925, was practiced generally before copper itself was welded to any great extent.

Welding Procedure—General.—The welding of copper by the oxyacetylene torch, the metallic arc, the carbon arc, the atomic hydrogen arc and the resistance methods are discussed below. Brazing is discussed in Part 8.

In planning a weld, the following items must be given consideration.

(*a*) Chemical, metallurgical and physical properties of the base metal as they influence the welding process and as they are affected by it.

(*b*) Selection of heat source, as for instance, the oxyacetylene torch or the electric arc. Its effect on the metals.

(*c*) Arrangement of the base metal. Preparation of edges and set-up for welding with suitable clamps or holding jigs.

(*d*) Selection of welding rod, composition and size.

(*e*) Selection of flux, if any.

(*f*) Laying of weld metal, i.e., actual welding operation.

(*g*) After-treatment of the weld or weld metal.

(*h*) Testing of the weld.

These items are discussed below for copper and the copper alloys in the light of published information.

Oxyacetylene Welding of Copper

The oxyacetylene torch is selected in preference to any other type of gas-air or gas-oxygen torch because of its well known high thermal intensity.

As will be seen from Fig. 1, the welding zone for copper (nearly the same as that for steel) is made up of hot hydrogen and carbon monoxide, both of which gases are strongly reducing.

Most of the torches today are designed to burn one volume of pure compressed oxygen with one volume of acetylene. For complete combustion, however, the acetylene requires two volumes of oxygen. The ambient air is drawn upon for this second volume of oxygen. This fact keeps the torch atmosphere for the most part free of oxygen.

Farther out in the torch flame we note in Fig. 1 CO_2 and H_2O both of which at normal temperatures are neutral gases. This does not, however, mean that they are neutral at all temperatures to all metals. Water vapor is decidedly oxidizing to some metals, to red hot iron, for instance. These gases are, however, much less reactive with most of the copper alloys than either hydrogen or oxygen. Elementary carbon is practically neutral to copper and insoluble in copper at all temperatures.

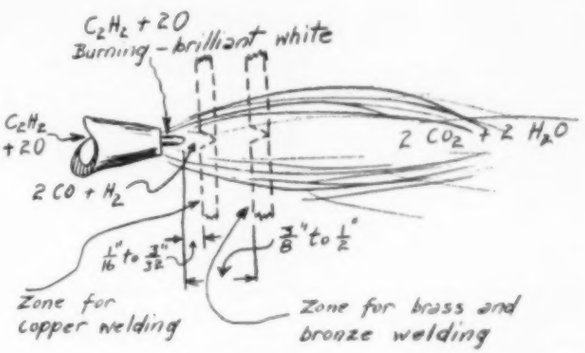


Fig. 1—Oxyacetylene Flame. (Adapted from Shimpke-Horn)[47]

The action of the torch gases has been discussed (Part 1, *Oxygen* and *Hydrogen*) and need not be repeated here except to emphasize the fact that oxygen-bearing copper is subject to two types of weaknesses—(*a*) the formation of the brittle cuprous-oxide-copper eutectic as a result of temperatures near the fusion point, and (*b*) the formation of sponge copper and inter-crystalline fissures as a result of the reduction of the cuprous oxide in the hot solid copper by hydrogen at temperatures between 700° C. (1292° F.) and the melting point of the copper.

For these reasons deoxidized copper is much safer to use in welding operations than oxygen-bearing copper. Numerous writers agree on this point. Among others, Percival,[60] Martin,[48] Le Grix,[9] Hibbard,[83] Miller[27] and the reviewer are convinced that deoxidized copper is preferable to oxygen-bearing copper when it is to be joined by welding. In England, phosphorous-deoxidized copper is made in commercial sheets and sold as "welding" copper.

Overcoming the Thermal Conductivity of Copper.—In gas welding copper, the melting temperature of small pieces of copper, 1083° C. (1981° F.) is easily attained by the oxyacetylene torch. On the other hand, it is difficult to raise the temperature of a small welding area in a large sheet of copper to the fusion point on account of the rapid conduction of the heat away from the welding zone to the more remote parts of the sheet.

To overcome this handicap, four things may be done —(*a*) cover the copper with heat insulating material, as, for instance, asbestos sheet, (*b*) preheat the copper with charcoal, low-cost gases, etc., (*c*) dispose the torch or torches so as to transfer the heat into the weld area as quickly as possible and (*d*) to increase the size or number of welding torches.

Of these four things (*a*) is perhaps the least effective. It saves heat, as the asbestos will reduce materially the radiation losses from the large areas. And it adds to the comfort of the operator. But welding cannot proceed until sufficient temperature is built up in the remote areas of the sheet to cause a slowing of the heat flow away from the weld areas.

Method (*b*)—the use of preheat—is often the most economical and practical way to overcome the high heat conductivity. It is sometimes difficult to apply to large vessels with relatively thin walls owing to the danger of weakening the copper to the point of allowing it to distort of its own weight. In many instances, however, preheating in a furnace, with charcoal or with city gas in an improvised furnace, will be found desirable and in some cases necessary. The writers are generally agreed on these two points.

Martin,[48] however, prefers the use of two oxyacetylene torches to preheating with coke or charcoal. He remarks, "It is a common mistake to preheat to too high

a temperature; it should never be necessary to raise the work to above a black heat."

Method (*c*) in which the torch is directed in a manner most conductive to the transfer of heat from the burning gases to the weld area is commonly used in copper welding but less frequently in the welding of steel or brass. It is known as "back-hand" or "left-hand" welding and is illustrated in Fig. 2. In this case the speed of the heat transfer, burning gases to weld metal, is a maximum since the torch blast is most nearly perpendicular to the weld pool. On the other hand, the actual efficiency of the heat transfer is probably better in fore-hand welding, Fig. 3, since the spent gases flow along the plate ahead of the weld preheating it so that the actual welding progress is faster with less danger of burning the metal than is the case with back-hand welding. Finally, the weld ripples are as a rule less pronounced and the weld seam smoother with fore-hand welding. Accordingly, in thin copper where it can be used, fore-hand welding is preferable, while in thicker copper in which the heat loss to the remote areas is great, back-hand welding must be resorted to. Hunsicher[49] in an extended investigation on the welding properties of 10 mm. thick (0.394 in.) electrolytic copper, obtained conflicting results when comparing right-hand welding with left-hand welding but concluded that the same favorable results were not obtained in the right-hand welding of copper as in the right-hand welding of ferrous metals.

As for size of power of torch to use, Martin[48] gives the following table showing approximate values:

Table 4—Data for Oxyacetylene Welding of Copper

Thickness of Plate Inch	Size of Welding Torch*	Preheater Torch*	Method	Gap in V at Start
1/16	225	..	1 welder	Nil
1/8	350	..	1 welder	1/16
3/16	500	750	1 welder 1 preheater	1/8
1/4	750	750	1 welder 1 preheater	1/8
3/8	1000	1000	2 welders	3/16
1/2	1000	1000	2 welders	3/16
3/4	1000	1000	2 welders	3/16

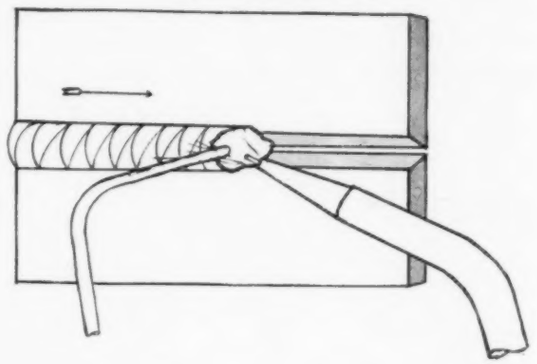
* Liters per hour.

The use of two welding torches, one to preheat and one to weld, or both to weld is many times a desirable means of overcoming the heat conductivity of the copper. In cases where the seam can be welded from both sides vertically as illustrated in Fig. 4 and described in detail by "La Soudure Autogene,"[50] we obtain the greatest economy of welding gases and time. It will be noted that both torches are used in fusing the weld metal and both progress upwardly.

In cases where the two torches are used on the same side of the plate, only one may be used for welding, the other preheating the seam and neighboring areas about 4 in. ahead of the welding torch, or both may be used for welding, as is indicated in Table 4. When both torches are used for welding, the first torch moves 2 to 3 in. ahead of the second depositing a relatively thin bead at the bottom of the Vee. The second torch takes advantage of the heat left in the plate by the first torch and makes a much heavier deposit.

Preparation of Copper Base Metal.—The principal concern in the setting-up of the base metal has to do with (*a*) preparation of edges, (*b*) positioning of edges and (*c*) backing-up of weld.

Fig. 2—Back-Hand Welding



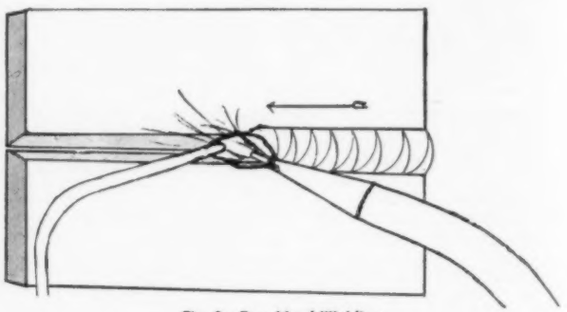


Fig. 3—Fore-Hand Welding

The edges are turned up as in Fig. 5(*m*) for relatively thin sheets. The up-turned edges act as stiffeners, preventing the edges from warping ahead of the torch and furnish at the same time a uniform amount of weld metal as they are melted down to make the seam. The up-turned edges are usually clamped together and tack-welded at 6-in. intervals.

According to Eyles,[51] the square cut edges, Fig. 5(*n*), may be used up to 1/8-in. thickness of sheet, with the single bevel for thicknesses above 1/8 in., and the double bevel for 1/2-in. thickness and thicker. Martin prefers a 60° Vee for Fig. 5(*o*) for thicknesses greater than 3/16 in.

The opening between plate edges as set-up for welding should be increased in the direction of welding at rate of 3/16 in. per foot more or less depending upon the speed of welding and size and shape of plates. Only in the case of thin sheets with the up-turned edges are the abutting edges brought hard together.

Backing-Up the Weld.—Martin[48] suggests the use of 1/4-in. thick × 2-in wide dry asbestos board supported by an iron bar or angle. The asbestos should be grooved

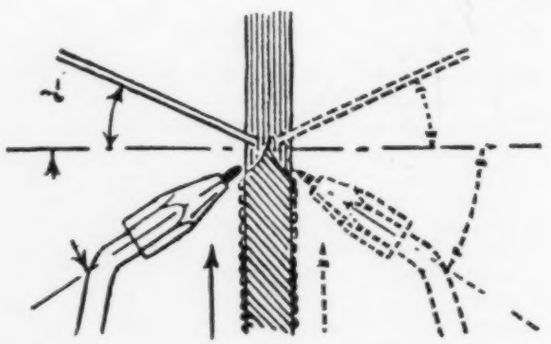


Fig. 4—Double Vertical Welding Torches Operating from Opposite Sides of Seam Simultaneously

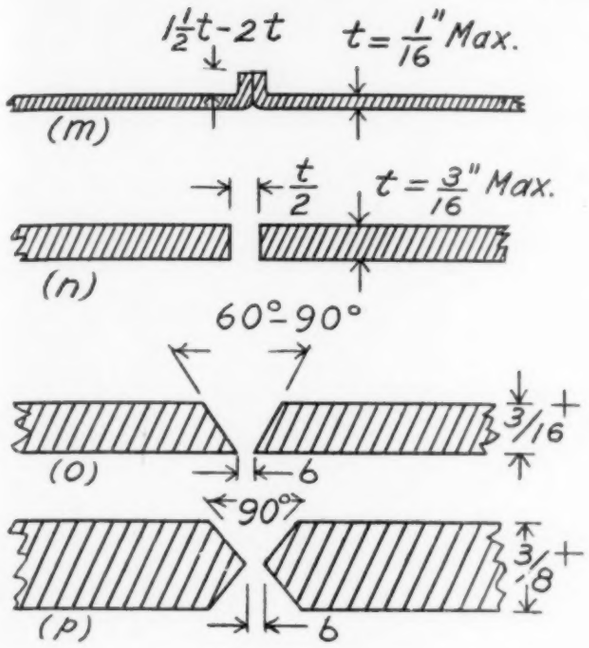


Fig. 5—Preparation of Edges

under the seam in order to allow full penetration of the plates while it acts as a mold for the highly fluid molten metal. In general, however, the vast majority of welding shops prefer to work without the use of a back-up.

Welding Rod.—The fact that the torch gases are reducing in nature has given many writers the idea that a deoxidized copper welding rod is unnecessary. Among others, Springer[63] in 1915 and Houlet in 1927 recommended "pure copper" welding rod.[52] Houlet reported 461 copper fire-boxes as being repaired by welding up to June 1927. Renaud[53] also reports the use of electrolytic copper welding rod in French State Railroad Shop. His welds, however, show only 33% efficiency with 5% elongation.

Saccomani and Verzillo[54] obtained much better results with electrolytic copper welding rods on welds that were hammered hot and annealed at 600° C. (1112° F.) They were able to show 20 to 22 kg./sq. mm. (28,500 to 31,300 pounds per square inch) with 29 to 31% elongation. They concluded that the extensive use of electrolytic copper welding rod was justified, though they obtained appreciably better results with a silver-bearing, deoxidized copper rod. They report 924 locomotive copper fire-boxes on the Italian Railways as being repaired by welding between 1924 and 1927, of which 332 were welded with electrolytic copper welding rod and the remainder with the silver-bearing, deoxidized copper rod. In the first 500 locomotives thus repaired 5% showed minor cracks in the welds, some of which were noted in welds made with the electrolytic copper rod and some in those made with the deoxidized copper rod.

Smith[55] shows that deposits of copper weld metal can be made entirely free of oxide by the use of the oxyacetylene torch—a deoxidizer not being necessary. He shows, however, that phosphorus helps to attain the same end, i.e., oxygen-free copper deposits.

Harrison[56] who made quite an extended survey of copper welding in Europe about 1930, found the French welders using electrolytic copper welding rod, and the Germans using a silver-bearing, deoxidized copper rod. He ascribed the use of electrolytic copper welding rod in France as much to the difficulty of securing the deoxidized silver-bearing rod as to a preference for the former.

Deoxidized Copper Alloy Welding Rod.—Though it seems clear from the above that it is possible to use an oxygen-bearing, electrolytic copper welding rod, the preponderance of opinion is greatly in favor of a deoxidized copper welding rod. And further of a welding rod alloyed with a third element.

The purpose of the alloying element (other than the deoxidizer) is to (*a*) lower the melting point below that of the base metal, (*b*) reduce grain size in weld metal, (*c*) strengthen weld metal and (*d*) improve soundness of weld metal by reducing its solubility when in molten state for gases, or increasing its solubility for gases when solid (avoiding a large ebullition of gas as it freezes) or (*e*) protecting it from the hot gases by providing a continuous liquid film over the weld pool. Hilpert[67] recommends additions of silver, manganese or zinc with phosphorus as a deoxidizer for obtaining sound weld metal. See also Part 1.

The phosphorus-deoxidized, silver-bearing welding rod[57] seems to be generally accepted as the best alloy thus far developed for copper welding, though an active search is still in progress for a less costly and more effective third element than silver, and a deoxidizer as efficacious as phosphorus which will admit of greater hot ductility (reduce hazard of cracking in temperature range just below the freezing point).

Many elements have been recommended as additions to copper welding rods, but aside from the well known phosphorous-deoxidized copper welding rod with or without a small addition of silver, few have been made generally available to the industry.

Flux for Copper.—Spraragen[58] in a review of flux data in 1924 noted that "very little scientific effort has been made to analyze the functions of the fluxes, their best composition, their best method of application and the results attainable from their use."

Geldbach[4] remarks in 1931 that "only a few references in the literature deal with the mode of action of fluxing agents *in a scientific manner.*"

Numerous fluxes have, however, been proposed and recommended and again many times the use of a flux is decried. It is generally agreed that a thin wash of flux on the plates adjacent to the weld will help keep the copper clean. Boric acid, sodium tetra borate (borax), sodium chloride and sodium fluoride are the more common constituents of fluxes for copper.

Springer[63] recommended the use of anhydrous borax or certain silicates, as powdered glass, etc., mixed with water or alcohol and painted on the weld and 1 in. to either side. He recognized the important point that the flux *must melt but not vaporize* during the welding operation.

Hunsicher[49] concluded that flux had little effect on the micro-structure of the junction of weld metal and base metal. Eyles[51] found that special fluxes had little value. Martin[48] regards the use of flux on deoxidized copper plate and deoxidized copper welding rod as being desirable chiefly for preventing the formation of the black cupric oxide scale on the surfaces of the base metal adjacent to the weld.

Froelich[59] is of the opinion that "choice of fluxes may be under-rated as well as over-rated, depending upon the shape or thicknesses of the section to be welded, the quality of the copper handled and the skill of the welder." He believes the use of flux on light sheets up to $^1/_8$ in. as being advantageous while he regards flux as being necessary for the welding of thicker copper particularly if it be electrolytic copper. He reports most fluxes as "being mixtures of dehydrated borax, aluminum

phosphates and silicates. The first two deoxidize and protect the melted and heated copper, while the silicates, although adding to the protection afforded by the flux, serve chiefly to sustain the stability of the flux . . . at the high temperature of welding." Percival[60] also regards a flux as necessary. He avers that "borax alone is not sufficient and even with additions of sodium chloride, the flux is not wholly satisfactory." Saccomani & Verzillo[54] used flux of borax, boric acid and chorides of sodium and calcium with good results.

Remarks on Fluxes.—The reviewer agrees with some of the authors quoted above that we have very little scientific data on the use of potency of various fluxes. It seems that welds in copper, satisfactory for some purposes can be made with no flux but the preponderance of opinion appears to be that a flux is desirable and improvement in the weld results in its use. Various commercial mixtures of boric acid and anhydrous borax with or without additions of a chloride or a fluoride, and less frequently a phosphate or silicate, are in common use.

Welding Operation.—It should be understood that the welds are fusion welds and the base metal must be fused in order to secure the best union—weld metal to base metal. If the weld metal is melted onto the hot solid base metal, there will be a bond of some definite value between the two but, it is only when the base metal is melted that the full strength will be developed in the connection.

There are several ways of accomplishing this—(*a*) by actually fusing the base metal before the weld metal is added or (*b*) by adding the molten weld metal when the base metal is still solid but close to its melting point and superheating the weld metal until the base metal fuses on the contact surface. Shimpke-Horn[47] recommend method (*b*), at least for thick sheets. They suggest that the weld zone be heated sufficiently with the torch to the point of its incipient fusion, then the melting end of the welding rod applied. They conclude that with this method, the welding rod must melt at a lower temperature than the copper and be as free flowing as possible.

H. Martin[51] also recommends this method, i.e., that "the base metal be brought to a bright red heat and about an inch of weld metal fused thereon and superheated until the base metal is brought to the correct welding temperature."

The reviewer is inclined to favor method (*a*), fusing the sides of Vee before bringing the welding rod into play. Mashl[62] supports this view remarking that "the base metal must be at the melting point before the welding rod is applied. If the welding rod is applied earlier, one will get 'adherence' (low strength weld). Apply the rod at the same time the base metal becomes molten."

In the case of a weld made in two or more passes, not simultaneously, the starting point of the second pass should never be at an end of the seam. It should be toward the center of the seam or at least four inches from the end. Froelich[59] calls our attention to the fact that if mechanical stresses are to be avoided, the starting point of the second pass must be at some point, P, remote from the edge. Weld to nearest edge as indicated by arrow, come back to P and finish in the direction of arrow 2.

Had the second pass started at the edge, it is probable that while the metal were still hot it would be pushed apart like the opening of a pair of scissors by the expansion of the metal being heated two to three inches in from the starting edge. This crack is prone to follow the second pass for several inches. The starting of the second pass at P, Fig. 6, avoids this hazard.

Shimpke-Horn[47] recommend the same procedure (except that they proceed to the farthest edge first and to the nearest edge last) for the *first* pass which, of course, is desirable if the sheets are rigidly held. If, however, the first pass is run as a "free weld" (the unwelded edges allowed to draw together as the weld progresses), the reviewer sees no objection to starting at one end of the seam and welding same straight through to the other end.

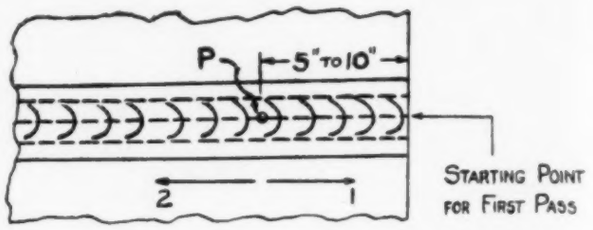


Fig. 6—Weld in Two Passes Showing Starting Point, P, of Second Pass

When heating a seam locally for annealing or hammering, the practice of Fig. 6 should be followed, i.e., start from a point several inches in from one end, proceed to the nearest edge, come back to the starting point and finish in the opposite direction.

After Treatment of Weld.—Since the weld metal solidifies and cools from a highly heated state, it is prone to have large grains. Moreover, since it shrinks from a highly expanded state while it is held more or less rigidly by its connection to the plates on either side, it is likely to have an appreciable value of locked-up stress. It is desirable, of course, to refine the grain of the weld metal, to make it more like that of the rolled sheet and less like that of a cast metal. It is also desirable to reduce the internal tensile stress since it may become additive to the service stresses causing unexpected and unknown working stresses.

In addition to the above two undesirable results, (*a*) large grains and (*b*) locked-up stresses, electrolytic or tough-pitch copper may have a third, and possibly a fourth, undesirable feature, (*c*) the segregation of the copper-cuprous-oxide eutectic and (*d*) a degree of hydrogen embrittlement hereinbefore described.

Annealing the Finished Weld.—In general a simple anneal in which the completed copper article is brought to a uniform temperature of say 650° C. (1202° F.), is neither helpful nor desirable. It will undoubtedly reduce the locked-up stress but any beneficial effect of an anneal, unaccompanied or unpreceded by mechanical work, on the grain size or oxide distribution, is doubtful. A good deal depends, however, on the impurities in the copper as to whether a simple anneal will or will not affect the grain size.

Leiter[10] claims a beneficial effect on the oxide distribution as the result of an anneal at 900° C. (1652° F.) and a prolonged period in the furnace. However, such an anneal, or in fact any anneal, of a large piece of welded equipment will call for carefully planned support of the copper to prevent its sagging of its own weight and a furnace with a neutral (nitrogen or carbon dioxide) atmosphere to prevent severe surface oxidation or hydrogen attack (if the copper be oxygen-bearing). Such elaborate furnaces are available to few coppersmiths.

Hence, except for small articles, or heavy compact castings such as blast furnace cooling wedges, the annealing of the complete, welded copper article is undesirable and unfruitful of sufficient benefit to make the trouble and cost worth while. Local annealing must, therefore, be resorted to.

Effect of Peening (Hammering), Forging of the Weld Plus Heat.—In this review we use the term "peening" to mean the mechanical working of the metal by small indentations as with the ball end of a hammer or a rounded

caulking tool. The term "hammering" is assumed to mean the working of the surface by the flat end of the hammer. Usually a weld is first peened to work a small area at a time without causing undue bending, followed by a hammering which smooths out the surface and eliminates the small indentations made in the peening operation.

Peening and hammering may be done (*a*) on the cold metal, (*b*) while the metal is hot from the welding operation or (*c*) when the weld has been reheated for the purpose of hot working it.

The effect of this working is well understood. Cold peening or hammering of copper raises the tensile strength and hardness at a sacrifice of the ductility. If followed or accompanied by a suitable temperature in the metal, it exerts a beneficial effect on all of the four ills arising from the welding operation, i.e., the grain is refined, the locked-up stress reduced or eliminated, the cuprous oxide is scattered as in tough-pitch copper and the hydrogen embrittlement ameliorated. The heat of correct temperature reduces the strength of the cold-worked metal to its inherent annealed value and restores the ductility lost by all the ill effects of welding and by the cold work.

Since oxygen-bearing copper is subject to all four ills enumerated in the foregoing discussion, and deoxidized copper only to the first two, it is more important to apply this corrective treatment—mechanical work plus heat—to electrolytic or tough-pitch copper than to the copper which has been thoroughly deoxidized and welded with a deoxidized copper welding rod.

Cuivre & Laiton[19] gives an excellent review of the literature on this topic, quoting numerous authors. There seems to be a general agreement as to the beneficial effect of peening the weld followed by an anneal or simply hot forging the weld, discontinuing the latter operation while the copper is still above its recrystallization temperature.

Martin[48] points out that "copper has an extremely low tensile strength when heated, so that the back of the weld should be effectively supported during hammering."

He states further that "plates below $^3/_8$ in. thick can be completely welded and hammered after they have cooled, but, above this thickness, hammering during the welding operation will lessen the risk of contraction cracks. Hammering should extend on either side of the weld in order to break up the large crystal structure caused through heating the plate."

Froelich[59] thinks that 0.2 in. should be the thickest sheet to be hammered cold and thicker than this should be hammered while hot. He suggests that the welds be annealed at 565° C. (1050° F.) and chilled with water to approximately 427° C. (800° F.) to improve ductility. The reviewer questions the efficacy of the water quench, as it will at the same time increase the hazard of locked-up stress. Eyles[51] supports the latter view saying—"It is essential to anneal after the cold hammering by raising it to 650° C. (1202° F.) and cooling slowly—the slower the better."

Saccomani & Verzillo[54] recommend a light peening hammer of about 400 grams (0.9 lb.) and a flat-faced hammer of 1kg. (2.2 lb.) to be used only when the copper is at red heat immediately after welding. They set the annealing temperature at 600° C. (1112° F.).

Conclusion.—Summing up the data on the oxyacetylene welding of copper, it appears that there is no unsurmountable difficulty to the making of welds in either tough-pitch or deoxidized copper. In either one, welds can be made that will approximate the tensile strength and ductility of the base metal.

In the case of the oxygen-bearing copper, it is vitally necessary (*a*) to hot forge the weld or (*b*) to cold hammer it and follow with an anneal if the maximum in strength and ductility is desired. Such treatment also improves the weld in deoxidized copper but is not so essential.

Arc Welding of Copper

The electric arc possesses some features which enable it to overcome copper's severest obstacle to welding operations, i.e., its high thermal conductivity. These features are (*a*) a very highly localized liberation of heat energy resulting in an extraordinarily high temperature, (*b*) a high proportion of the energy in the form of radiant heat and (*c*) an accurate quantity control of any desired amount of heat.

All of these characteristics of the arc contribute toward the overcoming of the heat losses to the remote parts of the copper base metal due to its high thermal conductivity. Thus (*a*) and (*c*) above put in the weldman's hand any desired quantity of heat at a high temperature in a small compass while the radiant energy strikes into the base metal faster than it is possible to deliver heat through an equivalent area by means of, for instance, a burning gas. Hence, the heat is actually poured into the copper at the welding area faster than it can be distributed by conduction.

Limitations of the arc.—There are, however, other factors limiting the application of the arc to the welding of copper. Thus, in the case of *metal arc welding*, the limitation lies (*a*) in the quantity of heat that the metal electrode can carry without having its weld metal burned or (*b*) without too great a loss of weld metal through "spluttering" i.e., droplets of weld metal thrown out of the welding zone by fierce magnetic and vapor disturbances. It is the reviewer's opinion that part of this difficulty can be overcome by the use of a heavily flux coated electrode of a suitable composition. The covering will protect the metal from burning by providing a neutral or reducing atmosphere in the arc and focus and stabilize the arc in a manner to diminish the magnetic disturbances. Such an electrode is, however, not available at this writing. Owing to limitations in the metal arc, the carbon arc has gained the greater following.

The second limitation in the use of the electric arc, be it with the metal or the carbon electrode, lies in the fact that the high temperature is prone to vaporize the more volatile constituents out of the copper. Thus, zinc, phosphorus and cadmium with their low boiling points will vaporize from the highly superheated weld pool leaving a weld metal quite different from the original alloy. In recognition of this fact, copper alloy manufacturers have developed welding rods for use with the arc which (*a*) have high boiling points or (*b*) in the case of the phosphor bronzes, an excess of the volatile constituent (phosphorus) to admit of the metals passing through the arc with an adequate residual amount.

Metal Arc Welding of Copper

Comparatively little information is available in the literature on the metal arc welding of copper. Martin[61] in a lecture to the Birmingham Sec. Inst. Metals in 1932 voiced the finding that it is necessary to use a bronze electrode with a heavy coating in order to avoid spluttering and to stabilize the arc. He concluded that welds made by the metal arc were inferior in quality to welds made with the oxyacetylene torch and were difficult to apply in a vertical or overhead position.

Weese[68] says there is little in the literature but reports some thesis work by Lindan at the Braunschweig Tech.

College who was unsuccessful in applying alternating current to the welding of copper. With the direct current arc of 30 to 38 volts and 130 to 180 amperes on a 3 mm. (0.118 in.) Canzler wire having a special coating applied by Elektrogen-Industrie (Hamburg) used as the positive metallic arc electrode, he obtained some fair welds. Also with a negative carbon electrode and a silver-bearing, deoxidized copper wire, he obtained some welds having 12 to 15 kg./mm.^{2} (17,000 to 21,000 pounds per square inch) tensile strength after forging. He was able to raise this to 22,500 to 25,000 pounds per square inch by annealing the welded plates four hours at 850° C. (1562° F.) and then forging same but the elongations were only 5$^1/_2$%. The reviewer believes that in general such treatment is not commercially practicable.

. . . . . .

In a summary on the arc welding of copper alloys in 1923, a committee of the AMERICAN WELDING SOCIETY headed by Bissell and Hobart[69] found the carbon arc in use as well as a phosphor bronze metal arc electrode. They found flux coated, tinned copper electrodes and copper electrodes containing silver or bismuth deoxidized with phosphorus suggested, but apparently not in commercial use. Sulphur, antimony, lead and copper oxide was found to make weld metal brittle.

This committee found that preheat to a dull red was necessary with the low capacity arc welding generators of the period. There was a disagreement as to the necessity or efficacy of hammering and annealing. Some recommended cold hammering of thin copper sheets followed by an anneal whilst thick copper sheets should be hammered at 500° C. (932° F.), annealed at the same temperature and quenched in water.

The only commercial application reported to the committee was the welding of the copper rail bonds to the steel rails in railroad signal and electrification work.

In 1926, Rouscher[70] succeeded in metallic arc welding some water cooled copper valve seats for blast furnace stoves which copper castings were 48 in. O.D. x 32 in. I.D. x 7 in. high with a 3 in. x 6 in. annular water space. His procedure included the Veeing of the cracks, sandblasting, annealing of the casting and quenching, preheating to a blood red, metallic arc welding with a bronze rod at 180 to 200 amperes and 30 volts, hammering lightly with flat hammer and again quenching. He remarked that the same procedure is used with smaller copper castings but the preliminary anneal and quench is unnecessary.

The high preheat used by Rouscher was also employed by Ribeaucourt[71] who found the best welding temperature to be 850° C. (1562° F.). His copper metal arc electrode was alloyed with 0.70% nickel in order to "stiffen" the weld metal. He used current values of 20 amperes per square mm. of electrode section.

Hasa and Benes[72] using as base metal electrolytic copper (99.956% copper) and metallurgical copper (99.540% copper, 0.151% arsenic, 0.101% Nickel) in 3 mm. (0.118 in.) and 8 mm. (0.315 in.) thicknesses and two different metallic arc electrodes (*a*) ordinary electrolytic copper wire (*b*) covered copper electrodes made by "Arcos" obtained welds of only moderate strength approximating an efficiency of 50%.

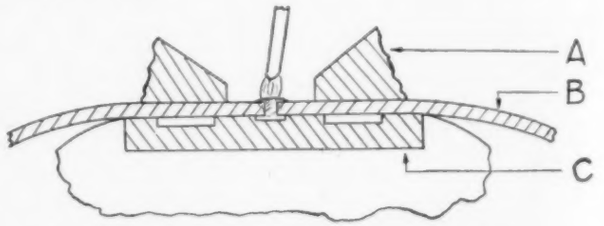
They regarded preheat as essential. They found it possible to improve the welds in the electrolytic copper base metal by hot forging and annealing, obtaining good strengths and ductilities in the latter welded with the covered electrode. They report copper as being weldable by either the A.C. or D.C. arc but that a covered electrode is essential with the former.

In 1929 and 1930, the reviewer[73] collaborating with C. E. Swift made an extended investigation into the metal arc welding characteristics of copper alloys using a D.C. welding generator of 500-ampere capacity and an automatic welding head. *Bare* electrode wires of (*a*) electrolytic copper, (*b*) silicon deoxidized copper, (*c*) Everdur, (*d*) phosphor bronze and (*e*) ambrac were tried. Tobin bronze was also tried but found unsuitable as a metal arc electrode on account of the volatilization of the zinc.

As a general conclusion, it was found that electrolytic or deoxidized copper sheet of $^1/_8$-in. (3.17 mm.) thickness or less could be readily welded with bare metal arc electrodes of silicon-deoxidized copper, Everdur or phosphor bronze, developing in the "as-welded" condition nearly the full strength of the base metal.

Bare electrolytic copper metal arc electrodes were found unsatisfactory, the weld metal from them being weak and unsound.

Set-Up for Welding.—Another feature that holds back the metal arc welding of copper as a fabricating process is the desirability, almost the necessity, for closely clamping the edges against a copper backer as illustrated in Fig. 7. This precaution is desirable on account of the fluidity of the metal. Even in a lap weld on sheets of $^1/_8$ in. or less in thickness a backer is required. In the absence of such a backer, holes are melted through the sheet which are troublesome and costly to repair.



A—Copper Clamping Bars. B—Copper Sheets to Be Welded. C—Copper Backer

Fig. 7—Set-Up for Welding Copper by the Metal Arc

In the case of relatively thick-walled copper castings such as those used around blast furnaces, Rouscher[70] warns that one must be careful not to chip a Vee all the way through. Enough metal, usually about $^1/_8$ in., must be left at the bottom of the crack to serve as a backer.

Walcott[74] calls attention to the fact that, "the metal arc welding rod, being used as the positive electrode, heats up faster than the negative base metal causing some metal to be deposited before the latter is sufficiently heated to obtain good fusion. He continues, "The starting point of the weld should, therefore, be preheated so that the welding operation can be started while the parent metal is still hot. . . . After a stop to change electrodes, the welding should be resumed at a point about $^1/_2$ in. back of the crater so that when the crater is again reached, the metal at that point will be sufficiently heated to permit the deposited metal to penetrate into the parent metal."

Walcott further recommends that "where possible, the weld should be made in one pass but where this is not feasible, the beads should be laid not more than $^1/_4$ in. thick and well peened while hot." Since Walcott is thinking of phosphor bronze as well as Everdur and silicon copper, the reviewer suggests that the above statement concerning "peening while hot" should be qualified to the extent of saying that the metal should be in the black heat range, i.e., 550° C. (1022° F.) or less, rather than in the red hot condition. The Everdur and silicon deoxidized copper can be forged red hot but such treat-

ment of a high-tin, high-phosphorus bronze is likely to develop hairline cracks.

.....

Summing up the possibilities in the metal arc field as they exist today, we may say that with the 400-to 800-ampere D.C. machines now generally available, there is ample heat capacity to do metal arc welding of copper with much less preheat than is called for above. However, there is still a lack of a satisfactory flux-coated electrode for the purpose. This fact, coupled with the troublesome set-up required, retards the process.

It should be pointed out, however, that the metal arc process has one feature which makes it desirable, i.e., once the correct heat is set, each unit of welding rod receives a uniform amount of heat (presuming, of course, a constant arc length and uniform movement along the seam). This is one feature that has helped to give the metal arc first choice as a method of welding in the structural steel welding field.

Carbon Arc Welding of Copper

To a large extent, the carbon arc welding of copper has taken precedence over the metal arc welding. In some respects, related to speed and economy, it has even outrivaled the oxyacetylene process. This development is of recent origin. The success of the process was found to hinge quite largely on a high-amperage, high-voltage arc which the earlier welding machines, built primarily for steel, were quite incapable of developing. As was stated in a previous article[75] an arc of 50 volts and 400 amperes or more is required for the successful carbon arc welding of copper.

Chaffee[76] appears to be the first to publish a description of the welding of copper by the high-power carbon arc. He remarks, "No metal can be fabricated more rapidly or at a lower cost than can copper by this method. It is now possible to arc weld for the first time ordinary copper economically, in the fabrication of tanks, barrels, vaults, kettles and other copper products." He credits W. C. Swift and Robert Bercaw with having developed the process in the opening months of 1933.

He describes the manufacture of a 500-barrel capacity brewing kettle made up of 1/2 in. and 3/16 in. thick electrolytic copper. A D.C. welding generator designed for the delivery of 400 amperes through a 40-volt carbon welding arc was used with a phosphor bronze welding rod high in both tin and phosphorus. Some tests showed a tensile strength of 23,000 to 25,000 pounds per square inch.

In another article[77] Chaffee describes the manufacture of large copper stills of 1/4 in., 3/8 in. and 1/2 in. thick electrolytic copper sheet by the carbon arc method. He regards an arc of 400 amperes at 40 volts necessary for welding 1/2-in. copper and high current and voltage values for thicker copper. Welding speeds of 12 to 24 in. of seam per minute with tensile strengths of 30,000 to 34,000 pounds per square inch are reported for the welds. The welds are not hot forged or annealed. Arc lengths of 3/4 in. to 1 1/8-in. are reported as being desirable.

A. F. Davis[95] calls attention to the economical fabrication of copper sheets by the carbon arc citing tensile strengths of 30,000 to 34,000 pounds per square inch obtained in copper sheets welded at 20 inches of seam per minute.

An interesting illustration of this method of welding is given in a description[78] of the repair of heavy cast copper, cooling plates used in a blast furnace. The castings weighing 195 pounds each are machined to a depth of 1/4 in. on the burned surface and a beveled, 5/16 in. thick, hot-rolled copper plate fitted therein, preheated over a coke fire and carbon arc-welded around the edges at 375 amperes using a 1/4-in. diameter phosphor bronze welding rod.

C. E. Swift[79] employing a high voltage carbon arc on electrolytic copper and deoxidized copper base metal in thicknesses from 1/8 in. to 0.20 in. obtained average tensile strengths and elongations in the "as-welded" samples of 29,861 pounds per square inch with 19.5% elongation in the former and 30,940 pounds per square inch with 43.3% elongation in the latter. It will be noted that these samples were welded against a copper backer with no preheat and the welds were given no after-treatment. The weld metal was a high-tin, high-phosphorous phosphor bronze.

In still another article, Chaffee[80] gives an extended series of tests of carbon arc welds on various thicknesses of *electrolytic* copper. In welds made with various commercial copper alloy welding rods and tested with the reenforcement intact, he obtained the following results:

Table 5—Test Results Carbon Arc Welds on Electrolytic Copper

Thickness of Plate	1/16 in.	1/8 in.	1/4 in.	3/8 in.	1/2 in.	Average of all tests
Tests on Base Metal, before Welding						
Tensile strength, psi	33,000	33,973	32,533	31,111	32,000	32,643
Elongation in 2 in., %	46.9	46.0	50.	62.5	62.5	53.8
Welds Made with Deoxidized Copper Welding Rod						
Tensile strength, psi	30,375	24,400	21,600	17,244	Weld	23,404
Efficiency, %	90.4	70.1	66.3	55.4	cracked	71.6
Elongation, %	14.1	10.2	9.4	6.7		10.1
Welds Made with Grade E Phosphor Bronze						
Tensile strength, psi	32,733	21,548	27,773	19,644	17,333	23,806
Efficiency, %	97.5	63.4	85.2	63.1	54.1	72.9
Elongation, %	17.2	9.8	14.9	7.9	9.0	11.7
Welds Made with Grade A Phosphor Bronze						
Tensile Strength, psi	30,719	28,213	10,799	22,644	21,566	22,900
Efficiency, %	91.4	83.	33.1	72.7	67.3	70.1
Elongation, %	15.2	14.	3.1	10.15	11.0	10.7
Welds Made with Grade C Phosphor Bronze						
Tensile Strength, psi	30,586	21,653	26,266	30,755	29,666	29,785
Efficiency, %	91.	93.1	80.7	98.8	92.7	91.2
Elongation, %	12.5	17.2	11.8	17.6	16.0	15.0
Welds Made with Grade D Phosphor Bronze						
Tensile Strength, psi	33,039	33,519	29,866	27,911	30,233	30,914
Efficiency, %	98.3	98.6	91.8	89.7	94.4	94.7
Elongation, %	14.1	23.8	14.9	13.7	16.0	16.5

Remarks.—While Chaffee has not made it clear as to the exact composition of the welding rods used, the reviewer assumes that he is using the Anaconda designation. If so, we have the following approximate compositions:

Welding Rod	Deoxidized Copper	Grade *E* Phosphor Bronze	Grade *A* Phosphor Bronze	Grade *C* Phosphor Bronze	Grade *D* Phosphor Bronze
	Composition (approximate)				
Tin	0.00	1.5	4.0	8.0	10.5
Phosphorus	0.05	0.05	0.20	0.05	0.30
Copper	Rem	Rem	Rem	Rem	Rem

From the efficiencies obtained, Chaffee concludes that the Phosphor bronze "*D*" rod is preferable to other rods having lower tin and phosphorus.

The reviewer suggests that the deoxidized copper, Phosphor bronze "*E*" and "*C*" may not have sufficient phosphorus to go through the high temperature of the carbon arc welding process and still retain a small residual amount. In general, the strength of the weld improves with the tin content.

It is probable that the heat setting was off the optimum in several welds, as for instance, those made in the $^1/_4$-in., $^3/_8$-in. and $^1/_2$-in. copper with the Grade "*A*" Phosphor bronze rod.

Chaffee offers no explanation for the poor results obtained in these same thicknesses with the deoxidized copper rod. The reviewer believes it possible that the phosphorus burned out of the weld metal allowing it to become oxidized. He agrees with Chaffee that *better results could have been obtained with deoxidized copper base metal.*

The results cited are indicative of what may be expected in the carbon arc welding of electrolytic and deoxidized copper. Unless welded with extremes in speed, 10 in. or more of seam per minute, the welds made in the oxygen-bearing, electrolytic copper will show decidedly inferior ductility to welds made in deoxidized copper at a much slower speed. As was pointed out before, this lack of ductility in welded electrolytic copper is due to the formation of the cold-short copper-cuprous oxide eutectic films which formation is a function of time. The longer the copper is held at a temperature above 920° C. (1688° F.), the greater the accumulation of the eutectic at the grain boundaries and the lower the strength and ductility.

It should be noted also that in general the carbon arc welding of copper is done most conveniently in a flat, or nearly flat, position. Hence, it is poorly adapted to the welding in situ of copper fire-boxes, etc.

An exception is in the welding of light gage copper with Everdur welding rod and a *short* carbon arc. In this case the high surface tension of the Everdur weld metal helps hold it in place, allowing vertical welds to be made.

While a copper back-up bar is not as essential in the carbon arc welding of copper as it is for metal arc welding, it is nevertheless very desirable especially with thin sheets and phosphor bronze welding rods. With Everdur welding rods, the high surface tension of the weld metal enables one to avoid the use of a back-up. Welds are almost invariably made as rigid welds, the plates being tack-welded together as a preliminary to running the first bead.

Flux in Arc Welding.—Apparently a flux is not essential in either metal arc welding or carbon arc welding of copper. This is particularly true when a phosphorized copper or bronze is used as the weld metal. However, it has been the reviewer's experience that a thin wash of a borax or boric acid base flux helps keep the base metal clean and improves the flowing of the weld metal.

After-Treatment of the Welds.—When using the high-tin, high-phosphorous bronze welding rod, no after-treatment is given. The welds are made usually at 10 to 20 in. per minute and left as welded. The phosphor bronze weld metal has sufficient chill-cast strength (about 42,000 pounds per square inch) to develop the strength of the copper base metal and the latter has sufficient ductility to absorb the locked-up stress.

Moreover, the phosphor bronze is not a good hot working metal. Hence, when it is necessary to work the weld metal, it should invariably be done cold or at a dark heat followed by a local anneal.

When the weld metal is a more ductile metal, as, for instance, deoxidized copper, the weld can be hot forged obtaining thereby improved ductility.

Testing the Welds.—Since the coalescence of the copper-cuprous oxide eutectic affects the ductility more than it does the tensile strength, the bend test affords the simplest measure of this quality. The weld may be subjected to a face bend (top of weld on outside of bend) or a root bend (bottom on outside). If the copper tends to peel away from the weld metal, there is a probability that the welding speed was too slow. This should not occur if the base metal is deoxidized copper.

Judging by the widespread attention that the carbon arc welding of electrolytic and deoxidized copper has received, it will probably help greatly the economical and speedy fabrication of copper. This is particularly true as the copper is welded rigidly with no preheat.

Atomic Hydrogen Welding of Copper

Only a few references on this method of welding as applied to copper are available at this writing. Cuivre and Laiton[19] quoting Darrat[81] says that "this process can be applied to the welding of deoxidized copper after a dark red heating. The filler metal must have the same composition as the base metal. The welds are sound, free from blow-holes. This process can also be applied to bronze and brass with the condition that the zinc content be less than 40%."

Martin[61] remarks, "Atomic hydrogen goes into solution in molten copper which, on cooling, releases the gas being honey-combed thereby." It is likely that Martin refers to electrolytic copper which, if true, would explain the discrepancy with Darrat's findings.

Weinman[52] gives a detailed description of the atomic hydrogen method as applied to the welding of non-ferrous metals but says little on the welding of copper.

The reviewer suggests that the process will have a degree of usefulness when its application to copper welding is more thoroughly worked out.

Resistance, Butt, Spot and Seam Welding of Copper

In straight resistance butt welding (as opposed to flash welding), spot and seam welding, the electrical as well as the thermal conductivity comes into play. Since the heat developed by an electrical current is expressed by RI^2 (where R = resistance in ohms and I = current in amperes), a smaller amount of heat is developed by a given current when the resistance is low as it is in pure copper. Hence, we have low heat at the same time that we have rapid conduction of heat away from the weld.

This double handicap may be met in two ways (*a*) by raising the current value and (*b*) by shortening the time of the current-on period. The first method should be effective since the heat varies as the second power of the current, but even so, an enormous current running into thousands of amperes, is required to compensate for the low resistance of the copper sheet.

Since a definite quantity of heat is required per spot weld, the shortening of the current-on time may seem at first glance to be an inefficient method of handling the problem. It means, of course, still higher currents. However, the heat loss by conduction to the surrounding metal is directly proportional to the current-on time. Hence, a shortening of this period means a proportional decrease in the heat loss. Ultimately, a compromise must be made as both the original cost of the equipment and the operating costs increase as a function of the peak current.

In general where copper sheet must be welded, a much higher capacity of welding transformer is required than is the case when welding steel. Woofter[84] remarks that 5 to 9 times as much current is required to weld copper as to weld steel. Thus, a 200-kva. capacity may be required where the heat pulses are cut to, say, $^1/_{120}$ second.[28] More often, it will be found desirable to use alloyed copper which will have a higher resistance than the pure metal.

In the case of resistance *flash* welding, a different situation exists. The resistance in the gap where the arcing takes place is much higher than is the case between metal to metal contacts. Hence, lower current values and longer periods may be used. Woofter[85] estimates that only 50 to 60% as much power is required for flash welding as for straight resistance butt welding. The requirements are that the faces of the copper shall be molten—not simply plastic—and that the final push-up shall be made quickly and lightly. Stranded copper rail bonds are flash-welded commercially to solid studs.

Woofter,[84] on the other hand, believes that the resistance butt welding of copper wires and rods is more practical than flash welding. He remarks that "the chief feature of a good copper weld is that it must be made in the shortest possible time. Slow butt welds show specks of both cupric and cuprous oxides. Such welds cannot be rolled or drawn." He agrees that a much lighter pressure in a decidedly narrower temperature range is desired for copper than is used for butt welding steel.

Woofter[85] is of the opinion that very thin copper sheets can be resistance spot-welded and seam-welded and possibly projection welded but at that time (1933) there was insufficient known data to establish the thickness limits, design of projections or transformer capacities and settings.

Another serious handicap to the resistance spot or seam welding of copper sheets pointed out by Woofter and others is the fact that the copper electrodes soften under the high heats and "mushroom" requiring frequent stops for redressing.

One frequent application of resistance butt welding is in the joining of copper rods to make longer lengths in the manufacture of copper wire. Hoffman[86] gives the details of the butt welding of such copper rods. "The ends are sawed square. A high capacity resistance welding machine which allows the welds to be made in $^1/_{10}$ second is described. A definite current and pressure is established for each size of rod. He reports 16,000 joints as being made in a 40-day period with 95 to 99% of the welds good, averaging an efficiency of 97% of the strength of the unwelded rod.

An interesting and rather unusual application of *pressure* welding is cited by Tucker.[87] In this case, clean copper and clean steel are brushed with aluminum powder or coated with aluminum by rubbing with aluminum brush, then placed together, heated and rolled. The aluminum under the heat and pressure bonds the copper to the steel. This is, of course, not an electrical weld.

Cuivre and Laiton[19] describes a simple expedient for overcoming the difficulties in spot and seam welding of copper sheet. They insert, between the two copper sheets, a thin sheet, say, 0.010 in. thick, of phosphor bronze which melts while the copper is at a red heat and bonds the copper sheets together. The reviewer believes that a reasonably high phosphorous content in the intermediate sheet will improve this action as the phosphorous vapor speeds the alloying action greatly.

Conclusion.—Thus, while it is possible to resistance spot and seam weld thin copper sheets, the reviewer is of the opinion that the manufacturer of copper products will find it desirable and economical to use a copper alloy having a higher resistance than pure copper.

PART 3

Copper-Zinc Alloys—Brasses

General

Copper forms with zinc alone, or with zinc and one or more additional elements such as tin, lead, manganese, aluminum, iron, nickel and silicon, an almost endless variety of useful alloys. These brasses and "bronzes," carrying zinc in proportions of 5 to 45%, by reason of a wide range of mechanical, chemical and artistic properties, have great commercial importance.

The zinc added to copper causes a progressive lowering of the melting point from the 1065° C. (1949° F.) of the 95 copper 5 zinc alloy to the 880° C. (1616° F.) of the 50:50 copper-zinc brazing, spelter solder.[88] Additions of tin to the brass will cause a further lowering.[89]

Zinc Vaporization.—Another general effect of zinc is to make the alloy as Geldbach[4] remarks "sensitive to overheating." In other words, the brasses when heated to a temperature above their respective melting points are subject to a loss of zinc by vaporization. Thorneycroft and Turner[90] show that there is an appreciable loss of zinc at temperatures as low as 600° C. (1112° F.), held for 30 minutes in a vacuum, from a brass carrying 75.12% copper and 24.88% zinc. This loss increases with the temperature until at 900° C. (1652° F.), it is 23.4% of the original zinc content.

It should, however, be pointed out that the "overheating" of which Geldbach speaks, causes no damage to the metal other than that occasioned by the loss of the zinc itself. The residual metal is still sound, strong and ductile though the proportion of copper in the alloy is increased by the loss of zinc.

Richards[91] in a theoretical discussion of the vapor tension of various brasses answers the question as to why, in brass casting at temperatures between 1000° C. (1832° F.) and 1200° C. (2192° F.), there is not a greater loss of zinc than there is, since zinc boils at atmospheric pressure at 930° C. (1706° F.). He explains that "there is a restraint on the loss of zinc due (1) to the molecular (atomic) proportion of zinc present of 15 to 30% which means that at any given temperature, the zinc can exert only 15 to 30% of the maximum vapor tension of pure zinc at that temperature and (2) the copper and zinc unite with some energy, and the energy of that combination also retards the volatilization of the zinc and lowers still more its vapor pressure. Owing to these two circumstances, the vapor tension of zinc from brass is usually less than atmospheric tension at operating temperatures, and therefore, the losses are not excessive."

Film-Forming Elements.—Another means of suppressing the zinc vaporization not mentioned by Richards has been developed recently in the welding industry, i.e., the use of films on the molten brass. These films may be developed in several different ways (*a*) by the addition of a suitable film-forming element to the brass alloy, (*b*) by the use of a suitable element in the flux or (*c*) by a suitable adjustment of the torch atmosphere.

The use of film-forming elements has been largely reviewed in Part 1. Amedeo[21] recommended the use of aluminum for this purpose in 1917 or earlier. Nothing better than aluminum[69] was known in 1923. Herrmann[96] also speaks of using aluminum for keeping down zinc vaporization in 1933.

Kinzel and Lytle[93] in a discussion on the use of silicon in brass mention the use of aluminum as follows: "Aluminum is another powerful deoxidizer which bestows some properties on the bronze similar to those given it by silicon, but it is not practicable to use aluminum in bronze welding rods due to the great difficulty of fluxing aluminum oxide."

The reviewer agrees with Kinzel and Lytle on the use of aluminum as a film-forming agent in bronze welding rods. While it keeps down zinc vaporization, it makes an unfluxable sheath on the weld metal which interferes with its bond to ferrous base metals.

Silicon, owing to the flux-like characteristics of its oxide when tempered with other oxides evolved in melting brass and bronze, makes a much better film for suppressing zinc vaporization.

Kinzel and Lytle[93] discuss the use of silicon in brass as follows:—"Silicon is valuable ..... as it is a powerful deoxidizer. It oxidizes to silica and protects the zinc and tin. Silica so formed is insoluble and floats on the surface of the molten metal. Also unites readily with bronze welding fluxes to form fusible boro-silicates and, as a consequence, the intimate flow of the bronze weld metal can progress unhindered.

"In silicon-containing bronze weld metal, the evaporation and subsequent oxidation of the zinc are considerably retarded due to the presence of the silica coating (film) on the molten surface. Experience has shown that 0.12% silicon is sufficient."

Method (*b*)—the use of film-forming fluxes—has not been exploited to any great extent though the boron base fluxes do make incomplete films. We are handicapped by a lack of published information on the effect of fluxes. One U. S. Patent[94] claims that silicon or a silicon-rich metal in finely divided form in a common flux will form a film over the molten brass thus protecting the zinc from oxidation and vaporization.

Gerbeaux[29] seems to be the first to call attention to method (*c*) in 1929. He says, "For welding ordinary brass, the flame should have a slight excess of oxygen so as to form a pasty mass containing zinc oxide on the surface of the metal, this prevents volatilization of zinc. Molten brass under an oxidizing flame is like soup containing milk. The latter forms a film on surface of liquid which prevents evaporation of water during cooking."

He recommends the use of $2^1/_2$ volumes of oxygen to 1 volume of acetylene whereas 1.2 volumes of oxygen to 1 of acetylene is more nearly normal for welding steel. This makes a *strongly* rather than a weakly oxidizing flame.

H. Herrmann,[96] W. B. Miller[92] and Shimpke-Horn[47] also recommend the use of an oxidizing flame for the welding of brass. Miller says further, "Treat molten brasses with oxidizing flux to obtain dense metal."

It will be noted that none of the above methods will guard the brasses from zinc vaporization when the weld pool is subjected to high superheats. The vapor pressure will increase with increasing superheat and no protective gaseous envelope or liquid or solid film will restrain the escaping zinc vapor. This fact explains why the use of copper-zinc alloys as metal arc electrodes has invariably been found impracticable.

The most logical general method of keeping down the zinc vaporization is to limit, as far as possible, the welding temperature to a value only slightly above the liquidus of the brass.

Oxy-Acetylene Welding of Brass

Brass is not difficult to weld with the oxyacetylene torch. Possibly for this reason, there is comparatively little published information dealing with it. Because of its lower melting point, lower heat conductivity and because it has a powerful deoxidizer, zinc, as an integral part of its composition, brass is decidedly easier to weld than copper.

Welding and Brazing.—The brasses as well as most of the copper alloys are readily joined by either brazing or fusion welding. "Brazing" in this review is understood to mean the joining of surfaces in contact by means of the copper-zinc spelter solders or bronze welding rods. The term in recent literature has been expanded to include the use of copper-phosphorous alloys and silver solders. The use of the latter is sometimes called "silver brazing." There is also some confusion as to the meaning of the term "brazing brass." Generally speaking, the term means a brass sufficiently low in zinc, usually 25% or less, as to admit of its being brazed with the 50:50 copper-zinc spelter solder without itself melting.

In brazing, the base metal is not melted whereas in fusion welding it is. In fusion welding, the welding rod is frequently of a composition similar to that of the base metal though not necessarily so.

Comparatively little has been written on the preparation of the base metal (brass) for welding of brazing. Usually for fusion welding the same character of edges herein before described for copper would be satisfactory for brass. For brazing, the contact surfaces should have a greater area than the right section of the brass sheet, shape or rod if it is to be depended upon for strength. This means in general an overlapped or scarfed joint.

Stegagno[109] investigated bronze welds of three types (*a*) the straight butt having a section identical with the right section of the specimen, (*b*) the usual 90° single Vee and (*c*) a shear type weld using steel, brass and Everdur base metals. On steel he obtained greater strength with type (*a*) than with type (*b*), on brass the strengths of these two types were about equal and on Everdur type (*a*) was again the strongest. He did not, make a brazed connection between overlapping or scarfed surfaces.

Welding Rods for Brass.—There is likewise comparatively little in the literature as to the exact welding rod or brazing metal to use with the numerous variations of brass. Frequently, conditions of color or corrosion resistance require the use of a weld metal similar in composition to the base metal. Where no such restriction exists, it will generally be found desirable to use a brazing metal or welding rod which melts at a lower temperature than the base metal. Usually such welding rods are high in zinc —38 to 42%. Fre- quently, they have a third element such as the tin, manganese or iron. Silicon is used in certain proprietary rods. These rods are reviewed in greater detail in Part 8 under *Bronze Welding*.

Seventeen copper alloy welding rods were listed by the reviewer[97] in 1931 is given on the top of the next page.

Not all of these welding rods are adaptable to the gas welding or brazing of brass—five of them are. Thus, common brass, Muntz metal, Tobin bronze, manganese bronze and the granulated brazing solder are often used on brass. The latter more frequently on brass carrying 25% of zinc or less in furnace brazing. The phosphor bronzes are frequently used for carbon arc welding of brass. And the ambrac and extruded and wrought nickel silver rods and granulated white brazing solder are used for the white nickel silvers and ambrac metals.

The welding rods applicable to the extruded brasses, bronzes and nickel silvers are further elaborated upon in an article on the welding of extruded bronze.[98]

Name	Copper	Composition, %	Melting Point Deg. C.	Melting Point Deg. F.	Average Strength of Weld Metal, psi
Electrolytic Copper	Rem	0.03 to 0.07 oxygen	1083	1981	15,000 to 28,000
Deoxidized Copper	"	0.01 to 0.07 Phos., etc.	1083	1981	30,000 max.
Common Brass	"	33 to 35 zinc	900	1652	32,000
Muntz Metal	"	40 zinc	885	1625	35,000
Tobin Bronze	"	38 to 40 zinc 0.50 to 0.75 tin	885	1625	45,000
Manganese Bronze	"	38 to 40 zinc 1 to 1½ iron 0 to 1.5 tin 0.03 to 1.25 manganese	870	1598	45,000
Phosphor Bronze *A*	"	5 tin 0.05 to 0.50 phosphor	1050	1922	38,000
Phosphor Bronze *D*	"	10 tin 0.05 to 0.50 phosphor	1010	1850	40,000
Wrought Nickel Silver	"	18 Nickel 27 zinc 0.15 manganese	1055	1931	35,000
Extruded Nickel Silver	"	8 Nickel 40 zinc 2.0 manganese	..	..	40,000
Ambrac	"	20 Nickel 5 zinc 0.50 manganese	1150	2102	30,000
Monel	"	67 to 70 Nickel 2 to 4 iron and manganese	1360	2480	....
Everdur	"	3 silicon 1 manganese	1020	1868	50,000
Brazing Solder, golden, granulated	"	50 zinc	868	1594	42,000
Brazing Solder, white, granulated	"	50 zinc 10 Nickel	895	1643	40,000
Phos. Copper	"	1 phosphorus	1070	1958	30,000
Phos. Copper	"	7 plus or minus phosphorus	710	1312	22,000

Escholz's 7.3% phosphorus 92.7% copper alloy[99] is used as a hard solder on copper on which the fluxing action of the phosphorus works best.

Viall[102] writing in 1920 speaks of Tobin bronze and manganese bronze as being satisfactory all around welding rods for welding brass and bronze.

Dawson[103] also mentions Tobin bronze and manganese bronze as being the most popular welding rods for brass in 1922 though he adds that brass is successfully welded with rods of the usual brass composition.

Flux.—There is comparatively little in the welding literature on the fluxing of brass. The use of borax and boric acid as fluxes for brazing brass was well established before the earliest fusion weld was made. Text-books of 1875[100] and earlier on mineralogy describe the dissolving power of borax and boric acid on the oxides of the copper alloys.

Herrmann[96] mentions besides boric acid and borax prussiate of potash, common salt, glass powder, finely pulverized sand, sodium phosphate and sodium ammonium phosphate as being acceptable ingredients of brass welding fluxes.

Wyss[101] reports welds in brass made with "Reuss" flux to have tensile strengths of 49,000 to 53,000 pounds per square inch. He does not give the composition of the flux but says it is made of "alkaline washings plus other ingredients."

Viall[102] sides with the majority in recommending calcined borax (fused borax or borax glass) or boracic (boric) acid as fluxes for brass.

The Bureau of Welding Committee[69] of 1923 states that mixtures of sodium chloride, anhydrous borax and boric acid make useful fluxes for brass and bronze.

Welding Operation.—There also appears to be a dearth of information as to the exact technique of brass welding and brazing in the literature.

Dawson[103] calls attention to a method of avoiding overheating of the weld metal. He says, "Melt small pool of base metal and insert rod of correct size in pool. Rod absorbs heat from pool and torch causing it to melt. The presence of the end of the rod in the pool prevents the temperature of the latter from rising in much the same manner that the temperature of water, containing ice, is held near to the melting point of the latter." As was pointed out before, if the temperature of the weld metal is kept close to its melting point, there will be little vaporization of the zinc.

Shimpke and Horn[47] point out another method of keeping the temperature of weld metal and base metal within proper limits. They say "Zinc vaporization can be avoided by first heating the base metal at the beginning place of the seam to the correct welding temperature and thereafter directing the torch on the welding rod." Thus, if the welding rod is the correct size, it will melt as fast as the base metal reaches the welding temperature and the weld will be completed without overheating either.

The reviewer in the case of the extruded brasses and bronzes[98] also calls attention to the importance of first bringing the starting point of the base metal to the welding temperature before the welding rod is applied. It is exceedingly difficult to pass enough heat through the weld metal to bring the base metal to the proper temperature without overheating the former.

This is even more important in the case of a braze when it is desired to have the brazing spelter penetrate into a lapped or scarfed seam.

Furnace brazing is perhaps an exception to this rule. In such practice, the base metal, flux and brazing spelter come to temperature simultaneously.

There is little information dealing specifically with the gas welding of Nickel silver. However, since these white metals carry from 20 to 40% zinc with 10 to 20% nickel, the remainder copper, their welding characteristics are not unlike those of brass.

The presence of 10 to 25% of nickel in nickel silver tends to make the molten metal dissolve gas, rejecting same when freezing to make blow-holes.

Viall[102] remarks "German silver (Nickel silver) in many cases is considered unweldable due to the absorp-

tion of gases. For practically all commercial purposes, it may be bonded, using the same flux as for brass and a strip of German silver for welding rod."

Tobin bronze and manganese bronze when used as base metals may be welded like high zinc brass. These two metals have 38 to 40% zinc.

The reviewer could find little on the after-treatment of brazes and welds in brass and Nickel silver. Herrmann[96] suggests the finishing of brass welds by many blows of a light hammer and annealing at temperatures not exceeding 566° C. (1050° F.) to 593° C. (1100° F.) and cooling in air.

A great many times when such materials are in process for manufacturing sheets, tubes and wire, such brazes are given cold work followed by an anneal which treatment refines the grain and restores the ductility. But in general, the absence of literature undoubtedly means that the connection is left in the as-brazed or as-welded condition.

One noteworthy application of brass welding is the welding of brass pipe. Among others Inskeep[110] in 1931 studied the effect of various types of joints as the Vee type butt joint and the short and long bell and spigot joint. He preferred the short bell and spigot to the butt weld or the long bell and spigot and described an installation of 90,000 pounds of red brass pipe (85 copper 15 zinc) in sizes up to 5 in. in which the short bell and spigot acetylene welded joint was used. He reports the use of a special brass welding rod having approximately 40% zinc.

Roberts[111] in 1933 also recommended the short bell and spigot oxyacetylene welded connection for yellow brass pipe. He remarks that "for connections in vertical pipe lines the short bell and spigot is easier (than the butt weld)."

Since 1933, however, with improving the skill in the operators, the trend has been toward the open Vee butt weld for brass and copper pipe for all sizes that have a wall thickness of $^1/_8$ in. or more. In an undated pamphlet[112] of about December 1935 distributed by the Linde Co., they say, "It was formerly supposed that the short bell and spigot type of joint would facilitate welding of vertical pipe such as risers because of the shelf supplied by the bell, but actual investigation proves that the butt joint can be as readily welded as the bell type in the larger diameters where the wall thickness is $^1/_8$ in. and greater. A suitable technique is to deposit the metal in a sloping puddle, directing the flame upward so as to retain the molten metal, at the same time manipulating the rod in the puddle to prevent excessive melting of the upper half of the joint and to solidify the puddle in even ripples."

"It is believed that the bell and spigot connection is not as efficient as the butt type joint ..... and as the cost of the bell and spigot joint, both in preparation and welding, is very much greater than the plain butt type for the larger diameters, it is recommended that the butt type joint be used for all brass pipe having wall thicknesses in excess of $^1/_8$ in.

For brass and copper pipe having wall thickness less than $^1/_8$ in., the reviewer has suggested a connection somewhat similar to a bell and spigot but which is adaptable to silver soldering or brazing as well as welding.[113]

Arc Welding Brass, Nickel Silver, etc.

It will be inferred from the foregoing that the zinc copper alloys are very difficult to weld with the electric arc on account of its high temperature and concentration of heat. This is particularly true of the *metallic arc* where the zinc alloy is used either as electrode or base metal. No film, flux or slag coating on a brass electrode is strong enough to suppress the zinc vapor pressure. It will suffer a considerable loss of zinc with interference in the operation from zinc oxide smoke and zinc oxide in the weld metal.

Wanamaker and Pennington[105] remark in 1921 "Brasses are not easily (arc) welded on account of the vaporization of the zinc content when subjected to the temperature of the electric arc. The addition of metal to brass is possible, but the use of a brass rod as an electrode is not."

Owens[106] in 1923 says, "Relatively little has been done to date on the development of either the metal or the carbon for the brasses and bronzes, and for that reason the oxyacetylene method still is recommended. The possibilities of the development of the carbon arc welding processes for the non-ferrous alloys seem to offer a wide field to any one having the facilities to pioneer in this work."

Carbon Arc Welding Brass

Another early reference to the arc welding of brass is made in the report of the American Bureau of Welding Committee of 1923[69] in which Mr. J. C. Lincoln is said to be using a carbon arc of 400 to 500 amperes with an open circuit voltage of 60 to weld brass rotor rings and rods with 2:1 brass rod at 6 to 12 in. per minute. Mr. Lincoln also uses carbon arc-welded copper rotor bars and rings at a rate of 4 to 8 in. per minute using brass scrap punchings for weld metal.

By the use of a zinc-free welding rod such as phosphor bronze, copper-silicon alloy or a cupro-nickel, and playing the arc from the carbon electrode to the zinc-free weld metal only, brass, nickel-silver and zinc-bearing bronzes may be welded quite satisfactorily.

Thus, Davis[104] cites numerous examples of arc welding in ship construction and repair. He speaks of repairing (*a*) breaks and worn places in trays and chutes, (*b*) welding of holes in the brass pilot houses, (*c*) tacking and welding brass flanges onto brass pipe for drainage lines, (*d*) welding hooks and rings to life rail stanchions, (*e*) building up bosses and worn places on machine parts, (*f*) welding copper tubing to brass union and (*g*) welding brass to steel.

Davis succeeded in welding vertically and overhead as well as in a flat position by this method. A considerable degree of manual skill is required to do this.

He must have caused some zinc vaporization in his work as he recommends ventilation in closed places by a blower during the welding operation to avoid having the operator get "spelter shakes."

Davis[104] continues further with some valuable hints on the procedure. As for instance he avoids construction difficulties by "welding a little at a time and peening each deposit."

He uses a phosphor bronze welding rod and a $^3/_8$-in. diameter carbon electrode. He recommends that about 15% more heat be used than for a steel rod of the same size or as much heat as can be handled and adds, "Novices fail on account of using too low a heat."

C. E. Swift[79] reports good strengths and ductility in carbon arc welds made in brasses containing 10, 20 and 40% of zinc respectively. He used a 10.5% tin bronze welding rod carrying 0.30% phosphorus, the remainder copper. He was careful to have the arc play between the carbon electrode and the zinc-free phosphor bronze weld metal. He also used a long arc which made a less intense heat on the brass base metal in spite of the high current values used.

Carbon Arc Cutting of Brass and Bronze.—The carbon

arc appears to offer the most practical method of heat cutting of the copper alloys which are not in general amenable to cutting by the oxygen cutting torch. Hagglund[107] tells of a unique and successful application of the carbon arc in which he cut the tips from two blades of a manganese bronze ship propeller under 20 feet of water at 33° F. The thickness of the manganese bronze varied from $^1/_4$ in to $1^1/_4$ in.

Resistance Welding of Brass and Nickel Silver

The reviewer has found[28] that brass and other zinc-bearing copper alloys are relatively easy to resistance spot, seam and butt weld when the conditions are such that there is no flashing (arcing). If flashing is allowed to take place, the rapid vaporization of the zinc tends to blow the molten metal out of the weld making a weak, badly marked or imperfect weld.

Woofter,[108] however, expressed a preference for the flash weld rather than the straight resistance butt weld for brass on account of the lower power required. He remarks that it should be made quickly. In another article,[85] Woofter remarks that flash welding requires only 50 to 60% as much power as resistance butt welding. He also says that resistance welds in brass require less pressure and more current than for steel.

Woofter[108] also remarks on the resistance seam welding of brass sheets to the effect—"Relatively thin sheet brass seam welds nicely. When welding 18-gage or 16-gage or thicker brass, the seam is somewhat ragged. By scarfing the stock, smooth, flush seam welds can be made on brass up to $^1/_8$ in. in thickness."

He finds an 80 copper 20 zinc brass rather difficult to resistance weld—probably because of the high electrical and thermal conductivity as compared to that of common brass.

PART 4

Copper-Tin Alloys—Bronze

Though the copper-tin metals are the earliest of all alloys developed in the metallurgical art, the literature on the welding of bronze is quite scanty. The reason for this dearth or information, is that there are comparatively few commercial applications of welded bronze. Thus, the greater part of the bronze having 10% or more of tin as the gunmetal bronzes, the bearing bronzes, art bronzes, the bell bronzes and the speculum metals are used in the cast form with little occasion for welding same. The more ductile bronzes, having less than 11% tin, are often used for their resilient properties in the cold-worked condition and any fusion welding heat would destroy or greatly diminish their elastic quality.

Also many of the bearing bronzes and valve metals carrying appreciable quantities of lead which make them hot short and, therefore, difficult to weld. The phosphorus used to deoxidize such metals and to make them free flowing in the casting molds also makes them hot short if it be present in proportions greater than a few hundredths of one per cent. Hence, special care must be exercised in welding operations.

Oxyacetylene Welding and Brazing

Three different gas welding or brazing methods are cited by different authors (*a*) a strictly autogeneous weld with preheat in the base metal, (*b*) a weld using no preheat and a yellow bronze filler rod and (*c*) a bronze using the 50:50 zinc copper spelter solder or the 80 copper, 5 phosphorus and 15 silver brazing alloy with some preheat.

Thus, in the welding of cracked bells which are frequently made of a hard cast bronze of 20 to 25% tin, the remainder copper, the crack is usually chipped out to make an open Vee and the entire casting preheated. Krez[114] describes the welding of a 6610 pound (3000 kg.) church bell using a 22 tin 78 copper filler metal while Meslier[115] tells of the repair by welding of five cast bronze bells. Meslier preheated to 600° C. (1112° F.) welded with a 20 to 25% tin bronze welding rod and the oxy-acetylene torch and cooled slowly.

Meslier adds that if a high zinc brass is used as the filler metal, the 600° C. preheat is not necessary. The weld may be made by local heating with the torch.* He states that the tone is not changed. This is in harmony with a statement made by a writer in the Canadian Foundryman in 1924[116] who welded cast bell metal bronze bells with manganese bronze.

The high preheat is avoided when the high zinc welding rod is used as the latter has a good degree of hot ductility which enables it to absorb the shrinkage strains with no permanent injury.

The third type of connection used with the tin bronzes is a sweated or brazed joint similar to that employed by Framburg in the manufacture of ornamental bronze lamp posts for a public building in Ft. Worth, Texas.[117] In this case, the yellow brazing spelter solder could not be used because of its high melting point but the silver-phosphorous-copper alloy flowed into the joint making a strong connection. A soft-flame gas torch was used to preheat the casting after which the sil-fos was flowed in with a small oxyacetylene torch. Borax was used as a flux.

A similar type of connection is described in the Welding Engineer[118] in which steam bronze header castings are assembled with copper tubes in the manufacture of a heater bundle. The cast bronze header and tube assembly is heated uniformly with a city-gas air burner and at the proper temperature, the silver-phosphorus-copper alloy is applied. Such assemblies are tested to 850 pounds hydrostatic pressure.

Torch Adjustment.—As in the welding of brass, the torch adjustment for eliminating vaporization or boiling of the bronze and the obtaining of sound weld metal is very critical. Roberts[119] in an exhaustive study of the various flame adjustments for welding both brass and bronze comes to the conclusion that an oxidizing flame will stop the boiling of the metal and produce sounder weld metal.

Roberts[119] says with respect to the bronzes, "When fusion welding the bronzes which contain relatively high amounts of tin or lead (and phosphorus), one or all of them, it will be observed that these constitutents start boiling† out before the base metal is even at a red heat. By using a strongly excess oxygen flame, however, for both the preheating and the welding, this tendency for boiling out of tin and lead (and phosphorus) is eliminated.

"After the base metal has melted and there is a noticeable film on the surface of the molten puddle, the amount of excess oxygen in the flame should be varied over a fairly wide range, during which it will be found that for one particular flame adjustment, the film or coating tends to disappear and a bright surface is maintained on the metal. This is the correct flame adjustment necessary for good welding of the high tin, high lead copper alloys. Sound welds free from holes or gas in-

* This is no different in its application from the bronze welding of cast iron, etc., described in Part 8.

† The reviewers would rather say "sweating" in this connection.

clusions and with well distributed tin and lead content can be made.

"With alloys containing relatively large amounts of lead, say over 5 per cent, some difficulty may be encountered due to the excessive formation of lead oxide, but by the use of an abundant quantity of flux painted on the welding rod, this will be largely eliminated.

"The same fluxes as for fusion welding of brass are found to be most satisfactory."

In an earlier article[120] the presence of lead in the brasses and bronzes is discussed as follows:—"Lead, if present in any appreciable amount, makes welding difficult and causes weak joints. In order to overcome this difficulty, an oxidizing flame should be used on brasses or bronzes having a high lead or zinc content. The flame adjustment is of the utmost importance in this type of welding. The flame first should be adjusted to neutral and then the acetylene supply reduced to form the oxidizing flame. It should be noticed that as the amount of acetylene is diminished, the flame at first shortens and then gradually lengthens and loses considerable of its luminosity. This last condition is the one necessary for successful welding of these alloys. When such a flame is used, a dense coating of slag is formed on the metal, and melting takes place beneath the coating. No boiling or fuming occurs during welding. . . . . a flux aids greatly in producing sound welds."

Welding Rod, Flux.—As was pointed out by several of the authors quoted above, a welding rod similar in composition to the base metal may be used or a high zinc yellow welding bronze such as manganese bronze or Tobin bronze may also be used. For brazing the silver-phosphorus-copper brazing alloy is most frequently mentioned. The 50:50 copper-zinc spelter solder gives trouble on account of its high melting point.

The fluxes mentioned most frequently are mixtures of fused borax and boric acid similar in composition to those used frequently with the brasses and copper.

After-Treatment.—Gas welds in bronze are rarely subjected to an after-treatment. The bronze is in general not amenable to hot forging. The weld can, however, be cold peened and annealed if grain refinement is sought.

Applications.—Besides those cited above, Sixt[121] reports the following successful applications of gas-welded brass and bronze.

Worn cast red brass bearing built up to original thickness. Preheated red hot in furnace to avoid distortion and to burn out the oily residue. Three layers of similar red brass weld metal applied by gas torch.

Acid resisting bronze castings welded with special bronze welding rod. Must avoid local over-heating in order to prevent vaporization of low melting constituents. Quick application of welding rod desired in order to offset vaporization.

Complicated valve seat in piston pump built up by wear-resistant bronze. The entire part preheated in gas furnace and slowly cooled after welding.

Tooth of bronze gear built up with bronze having 57,000 pounds per square inch tensile strength and 15% elongation but on 90 Brinell. The last layer was made of a much harder bronze.

Bronze propeller $3\frac{1}{2}$ feet in diameter was built up along the worn edges of its blades and one new cast blade was welded in place.

Badly pitted bronze turbine runner was repaired by flowing bronze over the surface using the welding torch.

Miller[122] tells of the welding in 1920 of a Francis type cast bronze water wheel runner from a 2500 H.P. 120 foot head turbine. It was removed from the pit, preheated and the pitted areas of the vanes welded with the oxyacetylene torch and a bronze welding rod. The result "was a good weld but a badly warped runner."

The reviewer suggests that perhaps Miller used too high a preheat. F. P. McEnerney of Derby, Conn., in 1923 and 1924 repaired eight large bronze turbine runners without obtaining appreciable distortion. He used the oxyacetylene torch and Tobin bronze welding rod *without preheat.*

Arc Welding of Bronze

Bronzes carrying 2% or less of zinc and sufficient phosphorus to deoxidize the weld metal can be successfully welded with either the metallic arc or the carbon arc. Davis[104] describes numerous applications of the arc welding of bronze ship fittings.

Miller[122] in repairing bronze turbine runners used the metallic arc to avoid distortion obtained with preheated oxyacetylene welds mentioned above. He says, "Succeeding runners were repaired by the arc-weld method, using the same type of rod. This rod was approximately the same analysis as the material in the runners, that is 60% copper, 39.25% zinc and 0.75% tin (Tobin bronze). As zinc is very unstable under the high temperatures produced in the arc, a high "sputter" loss was encountered, and the weld deposit itself was somewhat porous and not too well bonded to the parent metal. However, this was the best rod available at the time.

"All things considered, these repairs were successful. No distortion of the runners was encountered and no further repairs necessary for eight or ten years, although pitting was resumed in the welded areas in two or three years.

. . . . . .

"Since 1930 Phosphor bronze (Metallic) arc welding rods have been used in making repairs on the bronze runners. This rod contains no zinc and melts at a considerably higher temperature than the bronze of the runners, thereby assuring good penetration and an excellent bond. These rods cannot be used overhead, however, so all runners repaired have been removed from the machine. The first runners welded with this rod have been in continuous service since 1930. On a recent inspection (1935) about the only pitting found was outside the welded areas."

Mr. Miller[122] also gives some excellent hints on the procedure, thus:—"In preparing the runners for welding, all pitted areas were chipped down to clean metal with a light pneumatic chipping hammer and, if cracks were present these were veed out right through the vane. In many cases, the vanes were so thin from pitting that quite large holes were left after the chipping operation.

"To facilitate welding where holes were present, a backing-up plate was made of $\frac{1}{4}$-in. copper formed to the exact contour of the vane and wedged in tightly so as to cover the hole from the back or inside. The weld metal will not bond to the copper so this backing-up plate can be used over again. The hole was then closed up with a first bead. Successive beads or layers were then put on, each layer being not more than $\frac{3}{16}$ in. in thickness. After every bead the weld was peened lightly to relieve any strains left by contraction of the weld deposit. To avoid excessive heating of any localized area and possible distortion of the runner, only one bead is run on the vane at a time. The welder then moves to another section of the runner to continue welding."

The reviewer recalls seeing a heavy bronze runner from a Niagara Falls turbine repaired in 1926 by Rudolph King using methods described above by Miller. King

used a Grade *A* phosphor bronze (approximately 5% tin) metallic arc electrode.

Lebrun[123] describes the use of the atomic hydrogen torch for the repair of worn and corroded manganese bronze ship propellers.

Resistance Welding

Phosphor bronze is relatively easy to resistance butt, spot and seam weld.[28] Its electrical resistance is relatively high and its thermal conductivity low both of which properties help in resistance welding. When there are appreciable amounts of phosphorus in the bronze, the phosphorous vapor is brought out by the welding heat. It acts as a flux to aid in the bonding of the bronze. Cuivre and Laiton[19] suggest the use of thin phosphor bronze sheet interposed between copper sheets to supply a suitable weld metal for resistance spot and seam welding of the copper. The one difficulty in spot welding phosphor bronze with copper electrodes is that the phosphorous vapor also tends to make the electrodes stick to the sheet.[28]

EDITORS NOTE: Parts 5 to 8 inclusive will be published in the March issue.

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THE ENGINEERING FOUNDATION
WELDING RESEARCH COMMITTEE

Sponsored by American Welding Society and American Institute of Electrical Engineers

Supplement to the Journal of the American Welding Society, March 1937

Fatigue Investigations

In January 1937, there was published as a Supplement to THE WELDING JOURNAL an exhaustive review of the literature on "Fatigue Strength of Welded Joints" involving the digest of some 585 articles on the subject. In commenting on this report the Welding Committee of the Verein Deutscher Ingenieure writes as follows:

"It is a worth while piece of work.... We compliment you on having clearly separated the variables in fatigue testing of welds and giving numerical data....

"We consider that the chapter on Methods of Design as used in different countries is very explanatory, as are also the chapters on Service Results, Boilers, Bridges, Tubes and Typical Joints. Particularly important is the very complete Bibliography which omits practically nothing."

This review of the literature is an initial step preparatory to the conduct of important investigations on the subject in this country. Two such investigations involving fatigue testing of large-size specimens are now being planned: one at Cornell University under the auspices of the Structural Steel Welding Research Committee of the AMERICAN WELDING SOCIETY, and the other at the University of Illinois under the supervision of the Welding Research Committee of The Engineering Foundation. The latter program will probably involve the raising of some $25,000 in cash, with contributed services and materials equal to about twice this amount.

Bridges

Stressing the need for investigations of this sort, the Bridge Committee of the AMERICAN WELDING SOCIETY authorized the following statement:

(1) Fusion welding and flame cutting have been generally accepted in Industry. They are both rapidly being adopted in building construction. Welding is increasing in use as a means of reinforcing and keeping old bridges in service and also to augment or replace riveting in the construction of new bridges. Specifications have been prepared by a Committee of Bridge Engineers, Technicians and Welding Specialists appointed by the AMERICAN WELDING SOCIETY for their application in all phases of bridge construction. The problem now before engineers is to produce acceptable data which will establish the strength and endurance of welded bridges.

(2) Ample data are available on the strength of welded joints in static structures such as tanks and buildings. There is a marked difference, however, in the behavior of joints subjected to rapidly changing, or even reversing loads, such as predominate in bridges. Data are available from European sources, principally Germany, and to some extent Belgium and England, on the endurance of welded connections subject to fatigue from various types of loading. Data from American sources, however, are needed using American designs, steels, filler metals, machines, processes and procedure. Such data should be based on pulsating, reversing and the various combinations of stresses resulting from moving loads.

(3) Sufficient information has already been developed to indicate that welded connections subject to fatigue should be designed with gradually flowing lines from one member to another and with gradual changes in moment of inertia of the members. The connections involve a knowledge of continuity, rigidity and elasticity and this information is available in one form or another.

(4) Several welded bridges have been built in this country and their proponents call attention to several advantages over riveted construction. Welded bridges eliminate maintenance of connections as welded connections become an integral part of the original members; that is, the members or parts are fused together so that there is nothing to become loose. Welded connections would also result in continuity of frames which would reduce the weight of the structure for any given loading.

(5) It is the belief of the Committee, which prepared the welded bridge specifications, that no more important piece of research lies ahead of the American Construction Industry than an adequate program to demonstrate, under American conditions, the strength of welded joints under bridge loading and the most economical ways of designing such joints to make full use of welding. It is estimated that the cost of such research will be about $25,000 and take about 3 years to complete.

(6) Welding of bridges is of interest to railroads, highway departments, manufacturers of steel, filler metals, flame cutting and fusion welding equipment. It is recommended that one or more outstanding American Universities be authorized to make the necessary tests of full size joints for the various forms of loading which produce fatigue, that such tests be conducted over a period of 3 years and be adequately financed by the interests which would be benefited. It is also recommended that the designs be made and tests carried out under the general supervision of the Permanent Bridge Committee of the AMERICAN WELDING SOCIETY or a Conference Committee appointed by the various engineering societies interested.

Resistance Welding Research

Although this is one of the oldest and most widely used processes of welding, the scientific aspects have not been so thoroughly investigated as the other forms of welding. We do not yet know optimum conditions of current, pressures, time of welding and size of electrodes for the various forms of resistance welding in the most common applications. Although these remarks are generally true for mild steel, they are particularly true for alloy steels and non-ferrous metals. A great deal of research work is needed on these and other fundamentals of welding.

It is proposed to hold a conference sometime in the near future to which will be invited those interested, including the users and manufacturers, in order to lay plans for conducting a worth while investigation on the most important phases of the subject.

News Items

The Welding Committee of the Iron and Steel Institute (London) have drawn up a schedule of proposed research on the basis of discussion elicited at their Welding Symposium in May 1935. The schedule is divided into the following major sections.

Section A. Investigations on Weld Metal and Parent Metal.

Section B. Investigations on Distortion and Residual Stresses.

Section C. Investigations to Determine the Strength and Load-Carrying Capacity of Various Types of Welded Joints and Connections.

Section D. Investigations to Enable the Establishment of Correct Principles of Design.

Section E. Investigations on Problems Related to, or Involved in, the Use of Welded Construction (Models, Fatigue, Standard Sections, Non-Destructive Tests, and Corrosion).

The research on welding is to be under the control of a standing Research Committee appointed by the Institute of Welding.

Welding Cast Iron

A Review of the Literature to November 1, 1936

By W. SPRARAGEN* and G. E. CLAUSSEN†

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SUMMARY

Methods of Fusion Welding Cast Iron

Gas

By gas welding is meant the joining of cast iron by means of a gas flame, almost always oxyacetylene, using a cast-iron filler rod. The welding may be done with or without preheating.

Preheating.—The main purpose of preheating in gas welding cast iron—for that matter in all methods—is to avoid the shrinkage stresses and attendant cracks which otherwise concentrate at the junction zone between hot and cold metal outside the weld. Another important consideration is the avoidance of the hard zone next to the weld. Most authorities recommend a preheating temperature of 700° C. Preheating also saves gases.

Type of Flame.—A neutral flame is generally employed although a slightly carburizing flame is sometimes advantageous, as, for example, when there is a deficiency in carbon.

Technique of Gas Welding.—The crack is chipped out to form a 75–90° V. Rust must not be allowed to form in the V. A little flux is spread over the starting point and the flame played on the V until the walls begin to

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melt. The metal of the outer walls of the V should not melt ahead of the metal at the root, otherwise there is no fusion at the root. The rod, dipped in flux, is then brought to a red heat and rubbed into the molten metal. The molten metal should never be dropped into the puddle if slag inclusions are to be avoided, nor should a cold welding rod be plunged into the molten metal if hard spots are to be avoided. When the surface of the metal appears dirty a small quantity of flux should be added. It is generally agreed that the tip of the blue cone of the flame should be kept from $^3/_{16}$ to $^7/_8$ inch away from the molten puddle to avoid hard spots and loss of silicon and carbon. Sand specks cause blow-holes if they are not brought to the surface.

Fluxes.—Early in the history of gas welding it was found necessary to use fluxes for cast iron. A great number of flux compositions have been suggested. The action of aluminum powder according to Kesper, is in the nature of a thermit reaction. The powder, when scattered on the red-hot metal gives rise to incandescence, the metal becoming fluid, and blow-holes are prevented. The presence of aluminum in the flux also prevents loss of silicon. A flux that is frequently mentioned contains sodium carbonate and bicarbonate; a typical example is 15% borax, 15% sodium carbonate and 60% sodium bicarbonate.

Filler Rods for Gas Welding.—Cast-iron rods are used for gas welding. The AMERICAN WELDING SOCIETY recommends C, 3.0–3.5; Si, 3–3.5; Mn, 0.5–0.75; S, 0.1; P, 0.5–0.7.

Alloy Cast-Iron Rods.—A British insurance company has found that alloy filler rods for cast iron often give undesirable physical properties.

Metal Arc Welding

Cast iron may be welded with ferrous electrodes in two ways: (1) hot (with preheating); (2) cold (without preheating).

Hot Arc Welding

Preheating.—The purpose of preheating in metal arc welding is the same as in gas welding. The preheating temperature is usually 500 to 700° C.

Technique.—The hot process being used mainly for repairs on a large scale, a mold made of carbon or graphite plates is built around the fracture, which has previously been chipped. Some authorities recommend the light peening of hot welds, others have found peening disadvantageous. Flux of about the same composition as for gas welding is generally used unless the electrodes are covered.

Electrodes.—Cast-iron electrodes are used for hot welding. The composition is practically the same as for gas welding. Small amounts of phosphorus, aluminum, titanium and silicon, have on occasions been found to be helpful. Currents range from 300 to 1500 amps., 400 to 600 amps. being common. The voltage is 40 to 70.

Cold Welding Process.—This process is used for welds that need not be machined and that are not too thick. It is usually employed in connection with studs.

Technique.—The technique of cold welding has been developed to offset the tendency of cast iron to crack when locally heated. The general principle involved in all cold welding is to build up the sides of the joint with a steel electrode before closing the joint itself with steel weld metal. The ductile mild steel can then yield under the contraction stresses imposed by the joining beads. The cast iron is built up by depositing curved beads or by welding intermittently. In both systems, cleaning or chipping away oxidized metal and slag is essential, and peening is desirable. Cold welding uses as little heat as possible and is essentially a multi-layer process, but the annealing effect of multi-layers is not sufficient to prevent the formation of hard zones.

Electrodes.—Mild steel covered electrodes are generally used in the cold process. The composition of the core of the electrode is seldom mentioned as important. The coating of the electrode may perform two functions: (1) it may shield the arc and weld metal (shielded arc or flux covered electrodes); (2) it may alloy with the steel core to produce a cast-iron deposit. The coating for the first purpose is apparently of the same nature as on ordinary mild steel covered electrodes, and is designed to avoid nitrides. Carbonaceous coatings are used for the second purpose. One authority has found a coating of 40 to 60% graphite, 1% barium carbonate and the rest carborundum, with a paste of water glass as a binder, to be particularly effective.

The amperage used for covered electrodes in cold welding is kept low in order to reduce heat, hardening and cracking. For example a $^1/_8$-in. electrode on $^1/_2$-in. material using 80–90 amps. is a common value.

Type of Joint.—Much of the success of cold welding depends on proper preparation of the joint. Studding is a common method. The size of studs and spacing depends on the thickness of the casting. Copper, lead or soft steel inserts are used to insure machinability on one side.

Carbon Arc Welding

The technique of carbon arc welding closely resembles that of hot metal arc welding. Preheating from 400 to 700° C. is used together with annealing or slow cooling. Graphite rods are generally used and a flux containing equal parts sodium carbonate and sodium bicarbonate is useful. Filler rods are generally the same as for gas welding.

Thermit Welding

In the Thermit process a sand mold is built around the chipped or machined fracture and the parts are preheated. Molten metal is then poured into the mold. Thermit suitable for cast iron is plain thermit (a mixture of finely divided iron oxide and aluminum) to which 3% ferro-silicon and 20% mild steel punchings are added. The punchings serve both to lower the temperature of the molten metal and to control the chemical composition. The Thermit weld metal is, of course, almost always stronger than base metal, depending on analysis.

Other Methods.—The use of resistance and atomic hydrogen welding is briefly referred to in the literature.

Welding Cast Iron with Non-Ferrous Filler Rods

There are two common methods of joining cast iron by means of non-ferrous filler rods. The generally accepted method of welding cast iron is with bronze, in which a bronze filler rod having a lower melting point than cast iron is deposited by means of the oxyacetylene torch. The second method is arc welding with non-ferrous electrodes usually having a higher melting point than cast iron.

Bronze-Welding

Preheating.—Preheating, although not always necessary, is usually applied to assure success. The preheating may be local or general depending on the casting. A black preheat is generally employed although in some cases preheating to 900° C. has been found advantageous.

Technique.—As pointed out by the International Acetylene Association, if the temperature of the base metal in bronze-welding is too low, the bronze does not spread out; if it is too high the bronze collects in little balls which are driven away by the force of the flame. To avoid overheating, the torch should be held at a smaller angle to the deposited metal than in welding, according to one authority. Bronze-welding of large joints should be done in several layers. The removal of graphite from the surfaces to be welded has always been considered important. Graphite may be removed by scaling the surface at 900° C. or by use of an oxidizing flame.

Type of Flame.—A slightly oxidizing flame is usually recommended.

Fluxes.—The flux should be oxidizing in character in order to remove graphite, and it should remove oxide films from the base metal, otherwise capillary flow of the bronze (tinning) will not occur without overheating.

Filler Rods.—The customary filler rod for bronze-welding is a brass containing 60 Cu, 40 Zn. If the zinc content is too low the hot strength is correspondingly low; if zinc is too high the deposited metal is too hard and brittle. Tin, iron, manganese and silicon, about 1%, are often added to improve the flowing characteristics, decrease fumes, deoxidize and increase the hardness.

Types of Joint.—The V need be only wide enough so that the torch can be inserted. Double V joints are used for thick sections. In pipe welding the shear V joint is recommended.

Metallurgy.—The bond is developed by intergranular penetration of brazing metal along graphite flakes or grain boundaries, by solid solution formation, and by tinning. Graphite, especially flake graphite, causes poor adhesion. There is considerable evidence to show that brazing metal penetrates the cavities left by oxidized graphite flakes.

Physical Properties.—In discussing the physical properties of bronze welds it must be remembered that the bronze deposit generally has a tensile strength of from two to three times the tensile strength of the fusion contact and in many cases a strength greatly in excess of the cast iron itself. The shear strength of the junction zone is about 25,000 psi and the tensile strength about 35,000 psi. In order to bring up the strength of the welded joint the shear V butt joint and elongated or shear fillets are resorted to. This gives greater area of fusion contact. The junction zone is not any more brittle than the cast iron itself.

Welding with Non-Ferrous Electrodes Having a High Melting Point

Technique.—Covered electrodes (reversed polarity, short arc) are always deposited on cold metal. Beads not longer than 2 inches are deposited and peened lightly. After every second layer the deposit is caulked. For gas welding bare rods are used with preheat and refined powdered borax.

Electrodes.—The usual high-melting-point non-ferrous electrode for cast iron is monel metal coated with deoxidizer and flux.

Metallurgy.—Micrographic study reveals no hard zones in arc welds with monel electrodes. Good monel welds in cast iron have a tensile strength of 25,000 psi.

Metallurgical Factors

Oxidation of Iron.—Since the melting point of cast iron is about 200° C. below the melting point of FeO, molten cast iron at temperatures below about 1300° C. is covered with a solid oxide film. If the solid oxide is intermingled with the molten cast iron, it is difficult to remove. Early adoption of fluxes to make the slag fluid, and of puddling, the end of the filler rod being held below the slag, shows that oxidation of iron was one of the first difficulties overcome in welding cast iron.

Composition of Casting.—The chemical composition of the cast iron to be welded is a major factor in welding. Low-silicon cast iron, with its small graphite flakes, is easier to weld than high-silicon because large graphite flakes do not provide a clear foundation for the weld. Cast iron containing over 0.9 to 1.5% Mn or over 1.8% Si is difficult to weld by any process, whereas welding is facilitated if the casting contains nickel, titanium or vanadium.

Microstructural Factors

Hard Spots.—There is unanimous agreement that the hard, unmachinable zones generally found in cold welds and in improperly executed hot welds consist of white or nearly white cast iron. Invariably the hard zones contain not merely excess cementite but ledeburitic or eutectic cementite, suggesting that the composition or rate of cooling has been unfavorable to the precipitation of graphite during or immediately subsequent to solidification.

Graphite Structure.—An examination of the microstructure of all welds in cast iron, with the possible exception of burn-on welds shows that the graphite structure in the weld is finer than that of base metal. At times it is possible to observe a thin layer of altered "grown" graphite in the junction zone at the boundary to which weld metal had penetrated, which seriously lowers the transverse strength. However, the graphite layer is in no sense an unavoidable defect in cast-iron welds.

Heat-Treatment.—It is common practice to anneal some cast-iron welds after welding for three reasons: (1) to remove shrinkage stresses, (2) to break down hard spots, (3) to decrease carbon gradient. Heat-treatment at 760° C. followed by water quenching has been known to improve the strength of gas welds beyond that of unwelded cast iron without decrease of cross-bend deflection.

Physical Properties

A good gas weld made in cast iron has approximately the same strength as the cast iron being welded. The same is true for arc welding (preheated). Cold arc welds made with either cast iron or mild steel are quite variable. Preheated gas and arc welds have approximately the same ductility as cast iron. Cold arc welds made with cast-iron or steel electrodes are definitely inferior in ductility.

Impact.—Arc and gas welds preheated and annealed have practically the same value as the cast iron.

Fatigue.—Gas-welded specimens give about 90% of the endurance limit of unwelded cast iron.

Repeated Impact.—Unannealed gas welds under repeated impact withstood approximately 40 times as many blows as unwelded specimens. Annealing reduced the number of blows withstood by the welds to approximately the same as that of the cast iron.

Welding Cast Iron to Other Metals

With special precautions gas (with cast iron or bronze), arc and resistance welding may be successfully used in joining mild steel to cast iron.

Flame Cutting Cast Iron

Oxyacetylene

Burning vs. Melting.—The present methods of cutting gray cast iron are based on the fact that cast iron cannot be burned in the sense that steel may be burned at a temperature below the melting point. At 2.25% C the ignition temperature coincides with the liquidus (1370° C.), and with further increase in carbon, cast iron can be burned only by overheating to 1400° C. Cast iron cutting consists of melting the cast iron and blowing it clear of the kerf by force of the oxygen jet.

Type of Flame.—Excess acetylene is always used.

Torch.—The torch must have large gas passages and a larger injector than for mild steel. The use of preheated oxygen is always desirable.

Starting the Cut.—With cutting oxygen turned off, and the heating flame adjusted with a slight excess of acetylene to prevent back-fire, the torch is held at an angle of 45° to the edge of the piece to be cut, and is played up and down the line of cutting until the metal is red hot. The upper corner is then quickly brought to a molten state and the oxygen turned on, the oxygen stream being directed 10 to 15° backward at first.

Technique.—Once the cut is started the torch is given an oscillating motion.

The Torch-Steel Method.—For thin castings a thin mild steel plate, a weld bead or a wire may be used to aid in the cutting.

Arc Cutting.—Cutting cast iron with water-soaked metal electrodes at 75 volts and high amperage or with the carbon arc is entirely feasible.

Welding Cast Iron

Introduction

CAST iron is a common material of construction and, in one or another of its several varieties, it is being introduced to increasingly severe service, such as in automotive crankshafts. Up to the present the welding of cast iron has been confined almost exclusively to repair jobs. In fact, during the period 1926–1933, the annual consumption of cast-iron filler rods apparently was less than in the period 1914–1926. But there has always existed the hope that cast iron and rolled products could some day be welded together, a hope that has not yet been substantially realized, but that, if fulfilled, would remove a considerable restriction from designers of machinery and mechanical equipment in general. Simultaneous developments of suitable cast iron and steels and particularly of welding tactics are necessary.

The present review of literature on cast-iron welding is intended to provide welding engineers with a convenient summary of the methods adopted and materials used in welding, bronze welding and fusion cutting of gray malleable (issued separately), and special cast irons, and of the metallurgical principles involved, as well as of the physical properties that have been secured. With the exception of the section on physical properties, the majority of the references used in the review are from the literature of the past ten years. The early literature (1895–1925) has been consulted but is not given a prominent place in the review.

Methods of Fusion Welding Cast Iron

In this review no effort will be made completely to describe the technique of cast-iron welding. A number of welding text-books and instruction leaflets give the necessary details. It is intended rather to summarize important literature on the development of technique in all welding processes. Each process is dealt with separately.

Several general reviews of cast-iron welding have appeared in the past few years. Needy[1] summarizes the present status of carbon and metal arc welding. A Polish[2] and a French[3] article give extensive general reviews, including numerous repair jobs; Stieler,[4] Tyler and Percival[5] briefly review cast-iron welding, and Smallman-Tew[6] deals with arc welding in general. The Metals Handbook[7] outlines present practice. Older general articles by Rogers and Ogden[8] (1927 oxyacetylene), by S. W. Miller[9] (1918), and a book on fusion welding of cast iron by Horn[10] (1927), still contain useful information. The British Cast Iron Research Association[11] has prepared a bibliography of 74 references with brief abstracts covering the period 1917–1935. Of the total number, 39 references are to articles published before 1925.

Gas

By gas welding is meant the joining of cast iron by means of a gas flame, almost always oxyacetylene, using a cast-iron filler rod. The welding may be done with or without preheating.

Preheating.—The main purpose of preheating in gas welding cast iron—for that matter in all methods—is to avoid the shrinkage stresses and attendant cracks which otherwise concentrate at the junction zone between hot and cold metal outside the weld.[227] Pearce[12] considers that the main purpose is to prevent chilling of the deposited metal and that shrinkage stresses are a secondary consideration. Nevertheless, although it is sometimes important to obtain a soft deposit, and, therefore, to prevent chilling, the usual opinion is that the more complex the shape of the casting to be welded, the more necessary is complete preheating at high temperatures. Kohrs[13] conclusively proved that a cast-iron frame of relatively simple shape could be broken by heating locally to 400° C.

Reininger[14] believes there is no general preheating temperature. For simple parts 300 to 400° C. is adequate; for gears and other complicated castings the preheating temperature is 600 to 700° C. To preheat above 700° C. is unnecessary and decreases the percentage of pearlite in the base metal. Reininger is thus in disagreement with Pearce who seems to favor preheating above the A1 critical point in order to aid in decomposing cementite. Vossler[15] and Johannesen[224] favor a cherry-red preheat, equivalent to 750–800° C., but Jackson,[16] Knoch[17] and Vivien[18] advocate a dark red: 650–700° C. Kohrs[13] prefers 600–650° C.; cracking occurs below 500° C. Miller[19] also recommends 700° C., but, according to Schneider,[20] the Gussolit process of gas welding uses only 350° C. preheat. Bardtke and Matting[21] had no success in gas welding cast iron with a cast-iron rod (3.4 C, 2.9 Si, 0.5 Mn) if base metal was kept below 1000° C. Even with a high preheating temperature, a large amount of flux had to be used and the torch given a rapid circular motion to obtain passable "brazes." The deposited metal had to be held molten a long time to allow escape of gases.

Faulkner[22] describes preheating practice for defective automotive castings. Large cracked castings are preheated in a conveyor furnace to 720° C., gas welded, and placed in an annealing conveyor furnace. Machined castings with cracks are preheated only to 535–580° C., apparently to avoid excessive distortion. Rapatz[23] states that preheating is necessary if the heat of welding is not sufficient to heat through the whole piece. Thie-

sen[24] believes that preheating is essential only for gears or ribbed parts. It is interesting to note that preheating of cast iron was adopted in 1904 to save oxygen and acetylene.

Preheating practice is given in detail by Hettrick[25] who supplies data on charcoal requirements for different sizes and weights of castings and welds, as well as on consumption of oxygen and acetylene, and filler rod, and on time and labor costs. Typical data given for a fracture 30 × 4½ in. are:

Line up and prepare	4 hr., 2 men
Welding Time	11 hr., 2 men, 1 torch
Gases	900 cu. ft. (oxygen and acetylene)
Cast-iron rods	175 pounds
Charcoal	20 bushels
Weight	8000 pounds

The advantage of charcoal lies in uniformity of heating. Preheat practice is also discussed by Wikoff,[26] and Gibbons[27] briefly deals with charcoal, coke and blow-lamps. Vossler[15] gives a detailed description of a charcoal furnace. An improvised charcoal furnace suitable for cylinder blocks is also described by Knez[28] as well as by a writer[29] in a French publication. Although Streb[30] favors coke fires, Holler[31] believes that forge fires give non-uniform heating and gives a detailed description of a charcoal furnace.

An adjustable appliance for locally preheating cylinder blocks is described by Peterson.[32] Preheating, which is at a good red heat, is begun on external edges, but welding is begun on internal edges, taper heating being important. Lehmann,[33] who recommends a smith's forge or a wood fire for preheating, states that if the casting falls below a red heat during welding it should be returned to the furnace, but repeated heating may cause serious dimensional changes. Preheating is unnecessary for welding small cracks, according to Wuttke.[34] The spot to be welded is preheated by torch with an excess acetylene flame. If simple repairs are welded without preheat, they should always be stress relieved at 650° C., according to Kohrs.[13]

Type of Flame.—Fetherston,[35] Raabe[325] and Knoch[17] recommend a neutral flame, whereas Hermann[36] and Baillon[37] use excess acetylene. Baillon's experiments actually showed no difference whatever between excess oxygen, neutral and excess acetylene except that a carburizing flame seemed to produce more graphite in the deposited metal. If the cast iron to be welded contains less than 3.3% C, according to Titscher,[38] it is better to use a flame with excess acetylene in order to lower the melting point of base metal and to add fluidity.

Technique of Gas Welding.—A good account of gas welding as applied to locomotive cylinders is given by Vossler.[15] The crack is chipped to 75–90° single V. (Knoch[17] uses 60–70° V.) Rust must not be allowed to form in the V. A little flux is spread over the starting point and the flame played on the V until the walls begin to melt. The metal of the outer walls of the V should not melt ahead of the metal at the root, otherwise there is no fusion at the root. The rod, dipped in flux, is then brought to a red heat and rubbed into the molten metal. The molten metal should never be dropped into the puddle if slag inclusions are to be avoided, nor should a cold welding rod be plunged into the molten metal if hard spots are to be avoided. In order to secure rapid melting the filler rod and the cone of the flame should always be kept together. The rod should be stirred around to aid mixing. When the surface of the metal appears dirty a small quantity of flux should be added. The general method outlined by Vossler was of course used with various modifications early in welding history. Lyall,[39] for instance, adopted the puddling method, but used mild steel studs as well. The object of puddling, according to Clark,[40] is to remove oxides and gases.

There is disagreement with Vossler's method on two points. First, Miller[19] believes that the filler rod should be melted under the surface slag but should not be stirred around. Second, Hönisch[41] and Buschmann[42] prefer the double V joint. First one side of the V, then the other is welded in thin layers. It should be noted, however, that the method employs mild steel filler rods and the areas before and behind the molten puddle are kept hot by auxiliary torches. Hönisch, who employs the method on slag pans 2 inches thick, burns out the X with a cutting torch. The thin layers permit escape of gas that is retained in thick layers. In the Gussolit[43] process, the flame is maintained horizontal so that the edges of the V do not rise above 850° C.; a silicon cast-iron rod is used. Granjon[44] describes a multi-jet torch to preheat the filler rod and Neese[45] states that such burners require a high-pressure gas supply. According to Knoch,[46] the heating of the tip during welding, which tends to give an oxidizing flame, is particularly disadvantageous in welding cast iron. Knoch[17] found that no sand molds were necessary in gas welding because the melt is viscous, but Röthlin[47] observed that gas welding can be done only horizontally on account of the fluidity of the melt.

It is generally agreed[48, 49, 50, 51] that the tip of the blue cone of the flame should be kept from 3/16 to 7/8 inch away from the molten puddle to avoid hard spots and loss of silicon and carbon. Sand specks cause blow-holes if they are not brought to the surface, according to Miller.[19] Fetherston[35] describes overhead welding with carbon molds. He recommended (1923) 3/16-inch rods for thicknesses up to 1/4 inch, 3/8 inch for 1/2 inch and over. Bibus[52] explains the use of wrought iron paddles to smooth off the weld metal while it is mushy. A method[53] for obtaining dense welds is to strew charcoal dust on the liquid metal. In cold welding by oxyacetylene, Holler[54] notes that it is customary to preheat locally with two auxiliary torches.

The use of gas and arc welding in combination for cast iron, suggested by Halleux[55] as advantageous in some cases, has been applied more recently by Faulkner[22] and an anonymous writer.[56] The latter uses mild steel electrodes to tack together the pieces to be welded, then preheats and gas welds, melting out the tack metal. On machined automotive castings, Faulkner advises that the ends of the cracks be drilled out. Electrodes are then used to fill in the 1/4-inch drill-holes and a little way along the crack. The weld is finished by oxyacetylene.

Several writers[24, 57] note the importance of removing grease from the castings to be welded. Castings impregnated with oil or grease are boiled 10 to 15 minutes in a cleaning solution. The heat of the torch cannot be relied upon to burn off the oil. According to Ronay,[326] cutting oils should not be used for machining weld kerfs on cast iron, nor should the kerf be flame cut. Castings impregnated with moisture should be baked to avoid blow-holes.[58]

There is complete agreement that cooling after welding should be slow. The welded casting may be allowed to cool in the preheating fire as the latter dies down. If the casting is a small one that has been preheated by blow-pipes it may be buried in sand after gas welding. Often the casting is placed in an annealing furnace after welding.

Fluxes for Gas Welding.—Early in the history of gas welding it was found necessary to use fluxes for cast iron. Schoop[59] in 1909 described a flux containing alkali chlo-

ride and fluorides, probably in the proportion 65% K Cl, 15% Li Cl_2, 20% KF. The fluoride was supposed to form HF which intensively attacked the impurities. Schoop noted that borax produced hard spots. The main purpose of a flux, as Fetherston[35] pointed out, is to remove oxides, and it was known[15] that the flux should not boil or foam otherwise it does not get to the molten metal.

A great number of flux compositions have been suggested.[60, 61, 62] Keel[63] stated in 1926 that sand, borax, carbonates, chlorides and aluminum powder are found in fluxes. The action of aluminum powder, according to Kesper,[64] is in the nature of a thermit reaction. The powder, when scattered on the red hot metal gives rise to incandescence, the metal becoming fluid, and blowholes being prevented. The presence of aluminum in the flux also prevents loss of silicon. A flux that is frequently mentioned contains sodium carbonate and bicarbonate; a typical example is given by Roberts:[65] about 15% borax, 15% sodium carbonate, 70% sodium bicarbonate. Granjon and Rosemberg[51] recommend equal parts of Na_2CO_3 and $NaHCO_3$ with additions of 10–15% borax and 5% precipitated silica. According to Booer,[66] this flux is used to glaze the rod. Wyss[67] mentions a "Universal" flux consisting of a water solution of alkalies similar to a pickle. Lüder,[68] however, condemned all universal fluxes as compromises; cast iron requires its own type of flux. Politz,[69] adopting Honda and Murakami's discovery that ferrous oxide promoted graphitization, found that the addition of 1.5% FeO to welding flux (composition not given) gave a soft weld metal. A flux of equal parts of sodium hydroxide and slaked lime mixed with water to form a cream gave good results, according to McOscar,[70] who, however, believed that the sodium reacted with the silicon in the cast iron to form sodium silicate. A flux having the property of removing silicon from the metal can scarcely be considered desirable. It was Vivien's[18] opinion that removal of silicon according to: $Si + Na_2CO_3 = Na_2SiO_3 + C$, was desirable, and that loss of silicon from the molten metal as a result of this reaction could be prevented by adding silica to the flux. He also recommended an expensive flux containing 50% MnO_2, 4–6% KCN, 10% Si, 0.3% Al, 17% NaO, 17% SiO_2. The aluminum aided fluidity, precipitation of graphite, and removal of oxides. McOscar claimed that his flux prevented rapid cooling. Knoch[17] and Stieler[4] in 1934 both mentioned powdered calcined soda as a suitable flux, and Keel[71] recommended that the filler rod be dipped in water glass before dusting with flux.

Informative experiments on the action of flux in oxyacetylene welding gray cast iron (2.95% C, 2.42% Si, 0.4 inch thick) were reported by Baillon[37] in 1934. Using a filler rod containing 3.0% C, 2.8% Si, it was almost impossible to get hard spots even with the worst procedure. By using a flux: 50% Na_2CO_3, 45% $NaHCO_3$, 5% SiO_2, hard spots were difficult to avoid. When the blue cone of the flame is permitted to touch the surface of the puddle the flux is stirred into the metal and the following reaction occurs:

$$Na_2CO_3 + Si + O_2 \rightarrow CO_2 + Na_2SiO_3 + \text{heat}.$$

With a borax flux the reaction is:

$$Na_2B_4O_7 + Si + O_2 \rightarrow 2B_2O_3 + Na_2SiO_3$$

Without a flux the deposited metal analyzed 2.90 C, 2.37 Si; with flux, 2.88 C, 2.12 Si. The analyses were obtained from drillings taken in the vicinity of hard spots. Baillon concluded that all alkali and alkaline earth compounds that are unstable at the temperature of the oxyacetylene flame favor the formation of hard spots (Fe_3C) by removing silicon. Hard spots are usually found at the edge of the weld because the flux is blown there by the torch. If the blue cone is not permitted to touch the surface of the metal the slag is not stirred in, and acts only on the metallic oxide on the surface of the metal.

Two types of fluxes are distinguished by Baillon: First, reaction fluxes which are strongly exothermic in reaction, and remove iron oxide by forming a fusible double silicate; second, solution fluxes, such as alkaline fluorides which favor the solution of oxides in B_2O_3. The latter are unfortunately endothermic. Stable alkaline silicates cannot be used because their melting points are too high. The ideal flux should be strongly exothermic in reaction and should contain no alkaline or alkaline earth elements. Baillon states that the problem of a flux for welding cast iron remains unsolved.

The most recent recommendations for fluxes have been made in 1936 by Reininger,[14] Kohrs[13] and Campbell.[72] Reininger's flux contains 80% calcined soda, 18% boric acid, 2% Si. Boric acid is used instead of borax because the latter tends to swell. Kohrs uses plain calcined soda for all purposes. In his text-book on metal castings Campbell recommends an alkali carbonate flux.

Filler Rods for Gas Welding.—Cast-iron rods are used for gas welding. The recommended analyses of several authorities are listed in Table 1.

Baillon found it difficult to obtain hard spots with a rod containing 3.0 C, 2.80 Si, but difficult to avoid hard spots with 2.8 C, 2.20 Si in the rod. The high-silicon content of Knoch's rods is exceptional. The lowest silicon content appears to have been 2% suggested by Roberts.[65] Knoch found that unless smooth rods were used heavy slags will be formed. The rods he recommends can be welded horizontally or vertically using a small flame. Reininger proposed the following formula: Si in filler rod minus Si in casting to be welded = 0.7%. Pearce[12] suggested the formula:

$$Mn = 1.7\ S + 0.3\%$$

for manganese content. He believes that phosphorus up to 1% facilitates melting of the rod and promotes fluidity

Table 1—Filler Rods for Gas Welding Recommended by Various Authorities

Reference	Date	Total C	Graphite	Si	Mn	S	P
S. W. Miller[19] (A. B. W.)	1922	3.0–3.5	.....	3 –3.5	0.5–0.75	0.1	0.5–0.7
Tibbenham[73] (cheap)	1934	3.25–3.75	2.55–3.05	2.5–3.0	0.6–1.0	0.10 max.	1.0–1.3
" (expensive)	"	"	2.75–3.2	3.0–3.5	0.5–0.8	0.075 "	0.60–1.05
Baillon[37]	"	3.0–3.5	.....	3.0–3.3	0.20–0.40	trace	0.2–0.5
Knoch[17]	"	2.5	.....	6.7	0.5	0.04	0.5
"	"	3.1	.....	4.5	0.6	0.06	0.7
Reininger[14]	1936	3.05–3.3	.....	2.09–3.36	0.34–0.56	0.070–0.076	0.20–0.60
German Welding Comm.[74]	1927	3–4	.....	2.5–3.6	0.5–0.8	0.1 max.	0.4–0.8

but is of doubtful value in view of adverse effects on mechanical properties. Notwithstanding the effect of Si in raising A_1, it is objectionable in excess because it raises the hardness and embrittles the ferrite. A high-carbon, high-silicon rod for oxyacetylene welding cast iron was patented as early as 1907. The test used by Kohrs[13] to distinguish a good welding rod is to melt the end of the rod in an excess acetylene flame. If the rod has a round, smooth cup on the end, it will produce soft, fine-grained welds free from porosity.

Alloy Cast-Iron Rods.—Alloy cast-iron rods for gas welding gray cast iron have been suggested by a number of writers. Pearce[12] believes that nickel, although less powerfully graphitizing than silicon, may be substituted for part of silicon. Nickel toughens the matrix.

A rod containing 3.0–3.5 C, 3.0–3.5 Si, 0.5–0.7 Mn, 0.5–0.7 P, 0.1 S, 2–2.5 Ni, 0.5 Mo has greater fluidity than unalloyed cast iron, according to the International Nickel Company (private communication, January 1937) and does not require puddling.

A patent[75] for a nickel-plated cast-iron rod has recently appeared and an anonymous writer also recommends 0.5 to 1.0 Ni, noting that the effect of cobalt is not known. Another patent[77] suggests the simultaneous presence of up to $2^1/_2$ Ni with 0.2 Ti, the silicon being $2^1/_2$–$3^1/_2$%. Titanium alone (Springer,[78] Booer[66]) has also been used. Titscher[38] has obtained good results with nickel-chromium cast-iron rods. Pearce[12] states that austenitic welding rods are good but that there is the danger of a martensitic zone. Gas-welding cast iron with a vanadium steel rod, according to Farr,[79] is no better and more expensive than bronze-welding. Vanick and Merica,[80] and an anonymous writer[81] have used Niresist rods successfully for welding ordinary gray cast iron. It was the experience of a British insurance company[82] that alloy filler rods for cast iron often give undesirable physical properties.

The technique of casting filler rods for cast iron has been described by Brobeck[83] and Tibbenham.[73] The former, who emphasizes the importance of seamless rods, mixes carbon dust in the molding sand, and paints the white end of the rod so that the welder will recognize it as the end to discard. Tibbenham uses an addition of soda ash in the ladle to remove sulphur and refine the graphitic structure. A flat mold yields a clean skin; if the skin of the rod contains impurities the tendency to hard spots in the weld is increased. A typical cupola charge for welding rods is 35% hematite, 20% broken castings, 20% steel scrap, 15% ferro-silicon (13% grade), 10% English III pig, according to a German writer.[84] Two other writers[85] have briefly dealt with the casting of welding rods.

Repairs. The literature of welding is overstocked with examples of gas-welded gray iron castings. The general principles involved in nearly all these examples are the same so that only a few typical articles are summarized below. Heydacker,[86] Heaton,[87] Verzillo,[334] and the Air Reduction Sales Company[88] discuss the repair of locomotive cylinders, Gibbons[89] the repair of automotive crankcases, Schimpke[90] the repair of textile machinery, Müller[91] describes the repair of a small vise using two torches for preheating, and Kinsey[92] gives details for a large number of typical repairs. Adams[93] describes the gas welding of preheated bed plates and engine frames, stating that arc welding is used for cracks that cannot be reached by gas. Kohrs[94,13] shows in detail how to weld a preheated housing and a number of other parts, and Hunsicker[95] also gives a detailed description of welding a rigidly clamped bearing cap, without preheat, and using right-hand welding. Holler[96] points out that sudden freezing of deposited metal is the chief source of hard spots. He[97] has been successful in welding cylinder heads by supporting them on a water-cooled pipe during preheating. Wuttke[34] shows that "cold" or "half-hot" oxyacetylene welding is applicable to minor repairs on cylinder blocks. Shrinkage distortion in welded cylinders is avoided by tamping full of damp foundry sand, according to Industrial Gases.[98] A number of successful repair jobs are illustrated by Taylor.[99]

Preference of the gas over the arc process for gray cast iron is usually expressed when small castings are to be welded, but a noted welding authority, C. F. Keel,[100] believes that gas welding is best for thick or water-tight castings. Walby[101] found that sandy patches could be puddled out with gas but not with the arc process, and Horn[102] states that less preparation is required for gas than for hot arc welding. Kohrs[13] is a warm advocate of oxyacetylene welding for all types of repairs.

Metal Arc Welding

Cast iron may be welded with ferrous electrodes in two ways: (1) hot (with preheating); (2) cold (without preheating). Welding with non-ferrous electrodes is discussed in the section on welding with non-ferrous filler rods having a high melting point.

Hot Arc Welding

Preheating.—The purpose of preheating in metal arc welding is the same as in gas welding. Ketchbaw[103] notes that preheating to 550° C. permits entrapped slag to rise from the melt. The preheating temperature 500 to 700° C. given by Schimpke and Horn[104] appears to represent general practice, although Türcke [105] advocates 800 to 900° C. Preheating to 500° C. was regularly applied to the metal arc process by Braune[106] who had used the process 8 years prior to 1901. There seems to be no preference between charcoal, coke, gas or other fuel for preheating providing forced draft and local overheating by sharp flames are avoided.

Technique.—The hot process being used mainly for repairs on a large scale, a mold made of carbon or graphite plates (electrical conducting) is built around the fracture, which has previously been chipped, as described by Schimpke and Horn,[104] and Stieler.[107] Ketchbaw[103] recommends the insertion of a piece of graphite under the chipped 45° V, and warns against splashing molten metal on cold metal if hard spots are to be avoided. Among others, Holslag and Warner[108] recommend the light peening of hot welds, but the British Engine, Boiler and Electrical Insurance Co.[82] found that hot or cold peening was not beneficial. Braune[106] was among the first to mention the addition of lumps of cast iron to the molten puddle to regulate the temperature. Kantner and Herr[109] give a brief analysis of some of the defects in hot welds that are detected by X-rays. Nyquist[110] states that the weld metal should be held at a white heat for 30 seconds by moving the arc from side to side across the completed weld to permit graphitic carbon to form. If the casting is impregnated with oil the deposited metal should be held molten as long as possible.

Flux of about the same composition as for gas welding is generally used unless the electrodes are covered. Neese[111] obtained the results shown in Table 2 in hot welding cast iron, 500 to 1000 amps. He concluded that silicon was useful, titanium had no effect, and manganese and carbon were disadvantageous. Witte[112] observed that titanium oxide (TiO_2), added to the flux, gives dense, crack-free welds. Bowers[113] found that

Table 2—Effect of Coatings on Hardness of Arc Welds. Neese[111]

Material Added to Flux	% of Weight of Electrode	Brinell Hardness of Weld
Ferro-Silicon	1.5	260
"	3.0	225
"	6.0	210
"	9.0	200
"	12.0	190
Ferro-Titanium	0.75	230
"	1.5	220
"	3.0	213
"	6.0	211
Ferro-Manganese	0.5	220
"	1.1	270

NaCl, added occasionally to the weld metal, removed small blow-holes.

Electrodes.—Cast-iron electrodes are used for hot welding. The composition is practically the same as for gas welding. Sasaki[114] found that phosphorus was an advantage because it lowers the freezing point of cast iron and aids in liberating gases. Hashimoto[114] has successfully used cast-iron electrodes with 1% Al and states that the electric resistance of the electrode is an important consideration. According to Coulson-Smith,[115] titanium is added to electrodes because it combines with nitrogen. A thin coating containing silicon has been found by Wundram[116] to increase the fluidity. The majority of writers, including Davis[117] and Stieler,[107] believe that the electrode should always be positive. Schimpke and Horn,[104] and Lewenz,[118] however, are of the opposite opinion, although in 1926 Schimpke[119] also believed that the electrode should be positive. Braune[106] found that if the electrode were negative the deposited metal was white and partly oxidized. Currents range from 300 to 1500 amps., 400 to 600 amps. being common. The voltage is 40 to 70. Table 3 by Wundram[116] relates electrode diameter to amperage.

Table 3—Current for Hot Arc Welding. Wundram[116]

Diameter of Electrode, Inch	Current, Amps.
0.31	300–400
0.47	500–700
0.59	600–900
1.0	1000–1500

Owens[120] found that ingot iron electrodes gave less blow-holes than low-carbon steel or cast-iron electrodes for preheated cast-iron flanges. It is remarkable that the weld metal and junction zone obtained with cast-iron electrodes were unmachinable whereas the low-carbon electrodes gave soft, machinable deposits. The number of globules per minute from 0.15 inch, high-silicon cast-iron electrodes was 612 at 90 amps. D.C. and 210 at 165 amps. according to Flamm.[121] His measurements were made on the basis of oscillograph records and were not entirely consistent.

Half-Hot Metal Arc Process.—This process, which employs the same technique as the hot process, except that the preheat is only sufficient to dry out the mold, 150–250° C., is used only for small castings. Sometimes steel electrodes are used. Bauer[122] describes a special process using mild steel covered electrodes (300 amps.) in which the edges, chipped to 60° V, are preheated to the point of discoloration. The completed weld is lightly peened.

Cold Welding Process.—This process is used for welds that need not be machined and that are not too thick, and is usually employed in connection with studs.

Technique.—The technique of cold welding has been developed to offset the tendency of cast iron to crack when locally heated. The success of the welder depends to a large extent on the type of joint, which is discussed in a following section. The general principle involved in all cold welding is to build up the sides of the joint with a steel electrode before closing the joint itself with steel weld metal. The ductile mild steel can then yield under the contraction stresses imposed by the joining beads. The process is described by several writers,[123] such as Hill[124] during the Great War, and in the most recent text-book by Schimpke and Horn.[104] The cast iron is built up by depositing curved beads or by welding intermittently. The Lincoln Electric Company[125] recommend that beads not over 3 inches long be deposited (about $^1/_2$ minute of welding), followed by cooling for several minutes. In both systems, cleaning or chipping away oxidized metal and slag is essential, and peening is desirable. Cold welding uses as little heat as possible and is essentially a multi-layer process, but the annealing effect of multi-layers is not sufficient to prevent the formation of hard zones.

Although Schwarz[106] considers deep penetration important for cold welding, most welders prefer to have as little penetration as possible consistent with welding. McOscar[70] believed that a long arc ($^5/_{16}$–$^3/_8$ inch with $^1/_8$-inch electrode) should be maintained until the base metal under the electrode is molten, after which the arc is shortened to $^1/_8$ inch. The long arc forms a shallow crater which is said to minimize carbon absorption. He also recommends a flux (equal parts) NaOH and slaked lime mixed in water to a cream) in case the electrode does not fuse readily. Neese[127] found that the composition of the casting (1 to 3% Mn, 0.08 to 0.20 S, 1 to 3 Si, 0.4 to 1.4 P) had no effect on the quality of cold welds, which was uncertain in any case. According to Owens[128] the porosity of cold welds can be remedied by coating the completed weld with sal-ammoniac paste or, according to a French welder,[125] by electrolytic treatment. Kenyon[129] reports the removal of excess carbon from the vicinity of the scarf by forming a puddle and flowing off the initial high-carbon metal by properly manipulating the arc.

Electrodes.—Mild steel covered electrodes are generally used in the cold process. The composition of the core of the electrode is seldom mentioned as important. Lavroff[130] gives the limits of composition as 0.03 to 0.14 C, 0.00 to 0.11 Si. McOscar[70] specifies soft drawn, pot-annealed Swedish charcoal iron, Lewenz[118] also recommends pure, soft-iron electrodes, which have good red ductility and penetration, and an anonymous writer[131] and Haramiishi[132] favor wrought iron. Lloyd and Primrose[133] note that up to 5% Si has been added to pure iron electrodes without particular success, and that an electrode with 1% C, and 20% Ni, gives hard spots.

The Lincoln Electric Company[117] have found 18–8 (0.07 C) useful. An investigation of austenitic electrodes for cold welding gray cast iron has recently been made by Batmanon.[134] The welded specimens were made of plates $1\ ^3/_{16}$ inches thick, 8 inches long, 6 inches wide. The electrodes and coatings had the compositions shown in Table 4.

Table 4—Electrodes and Coatings Tested by Batmanon[124]

	C	Ni	Mn	Si	Cu	Cr	P	S
Electrode 1	2.27	21.78	1.24	6.18	..	0.69	...	...
" 2	2.04	28.97	0.46	1.31	7.62	..	0.038	trace
" 3	2.33	22.09	0.54	6.41	..	0.15	...	...

	Fluor-spar	Marble	Ferro-Silicon	Graphite	Alumi-num
Coating A	15	85	..	..	..
" B	..	10	15	50	25
" C	..	10	45	45	..
" D	..	15	27.5	42.5	15

The tensile strengths ranged from 17,000 to 32,000 psi, the hardness from 192 to 255 Brinell. Multilayer welding was required to produce a machinable deposit with electrodes 0.28 to 0.31 inch diameter. Czyrski[336] used high-silicon austenitic electrodes. Hill[124] used alloy steel electrodes (no details) as early as 1918. High-silicon cast-iron electrodes are not good for cold welding, according to Steel,[135] because the arc is unstable. A nickel cast-iron electrode is recommended by Welder[136] for thin sections.

The coating of the electrode may perform two functions: (1) It may shield the arc and weld metal (shielded arc or flux covered electrodes); (2) it may alloy with the steel core to produce a cast-iron deposit. The coating for the first purpose is apparently of the same nature as on ordinary mild steel covered electrodes, and is designed to avoid nitrides. Carbonaceous coatings are used for the second purpose but *Nickel Cast Iron News*[327] (1937) knows of no commercial electrode of this type. The two major investigations of such coatings have been made by Shun-ichi Satoh[137] and by Goryatsev.[138] The former wound wrought-iron rods (0.19 inch diameter, 0.04 C, 0.054 Si, 0.09 Mn, 0.02 S, trace P) with asbestos twine (0.04 inch diam.), and coated them with a paste of water glass (1 part water glass to 3 parts mixture) containing silicon carbide or graphite and mixtures of the two. The weight of coating obtained by using a glass tube 0.31-inch diam. as mold gave the best results; narrower tubes gave a relatively low-carbon weld deposit. With the electrode positive the terminal voltage decreased from 25 to 20 volts as the graphite content was decreased from 100 to 0%, the current rising from 140 to 155 amps. to correspond. The chemical analysis of the deposits is shown in Table 5. Similar coatings were used by Jermilow.[338]

Table 5—Effect of Coating on Composition of Arc-Deposited Metal. Shun-ichi Satoh[137]

Coating—%		Weld Deposit—%				
Graph-ite	Car-borun-dum	Total Carbon	Graph-ite	Com-bined Carbon	Silicon	Man-ganese
100	0	2.546	0.221	2.325	0.914	0.09
90	10	3.073	0.973	2.100	1.289	trace
80	20	2.844	1.320	1.520	1.780	0.22
70	30	2.765	1.375	1.390	2.049	0.119
60	40	2.831	1.493	1.338	2.815	trace
50	50	2.989	1.875	1.114	3.641	0.103
40	60	3.634	3.356	0.278	4.258	0.043
30	70	3.177	3.011	0.116	5.819	0.073
20	80	3.240	3.240	...	7.630	...
10	90	2:820	2.820	...	10.600	...
0	100	2.748	2.748	...	11.580	...

The best coatings were those containing 40 to 60% graphite. But electrodes with these coatings could not be successfully used for cold welding unless the electrode was made negative and 1% barium carbonate was added to the coating. The time of melting of an electrode was increased from 3 to 5½ minutes in virtue of these changes. Shun-ichi Satoh ascribed the effect vaguely to the barium anion. If the electrode was positive additions of 3% $BaCO_3$, $CaCO_3$ or calcined borax had no effect.

The results of Shun-ichi Satoh were scarcely confirmed by Goryatsev and co-workers, the results of whose experiments are shown in Table 6. The coating was about 25% of the weight of the electrode.

Table 6—Effect of Coating on Composition of Arc-Deposited Metal. Goryatsev[138]

Coating Composition %					Deposited Metal Per Cent			Recovery Per Cent	
SiC	Fe-Si	Graph-ite	$CaCO_3$	$BaCO_3$	C	Si	Mn	Si	C
52.0	...	34.8	13.2	...	3.24	3.57	..	39.0	20.0
..	41.0	41.0	16.4	1.6	3.15	3.87	0.55	50.0	29.0
..	49.2	32.8	16.4	1.6	2.77	4.98	0.37	52.0	32.0
..	50.0	33.4	16.6	...	2.6	4.7	0.4	47.0	29.0

The ferro-silicon contained 72.5 Si, 0.11 C. As the table shows, the ferro-silicon is more efficient than carborundum. The bend and tensile strengths were also higher using a ferro-silicon coating than using carborundum. Welding with 160 to 220 amps. both coatings gave about the same maximum hardness: 370 Brinell, minimum 170 Brinell. The maximum hardness was reduced to 265 Brinell by slow cooling.

Another coated wrought-iron electrode designed to yield a cast-iron deposit is briefly described by Haramiishi.[132] The reaction between core and coating was extremely rapid, for the melted tip of the electrode was white cast iron. In 1922 Kjellberg[139] described a coating containing carbon, aluminum and silicon as deoxidizers and ferro-cerium to increase the fluidity of the bath so that evolved gases can escape. Buchholz[140] has also discussed the function of aluminum, manganese and silicon in the coating. Haglund[141] describes a covered steel electrode containing 0.1–0.2 C, 1–4 Ti. and (or) Zr, 0.2–4.0 Si, and some Mn or Al to protect the Ti from oxidation. The coating contains 10–40% graphite and no preheat is required.

The amperage used for covered electrodes in cold welding is kept low in order to reduce heat, hardening and cracking. Schimpke and Horn[104] give the values shown in Table 7.

Table 7—Electrodes for Cold Welding. Schimpke and Horn[104]

Wall Thickness of Casting, Inch	Voltage	Amperage	Diameter of Electrode, Inch
0.20	22	120	0.12–0.16
0.40	24	150	0.16
0.60	26	160	0.16
0.80	30	180–190	0.16–0.20
over 0.80	35	190–200	0.20

The Lincoln Electric Company recommends 80 amps. D.C., reversed polarity, for its Ferroweld electrode (⅛ in. diam, steel core with heavy flux coating, shielded arc) for cast iron. Welding should be done very intermittently to keep down the hardness, but with short beads (not over 3 in. long) and peening the bond is excellent.

The amperage of the Quasi-Arc electrodes is particularly low, being 35 to 45 amps. for 0.11-in. electrodes, and 95 to 115 amps. for 0.17-in. electrodes. The polarity is reversed. Sarazin[142] is in better agreement with American practice than Schimpke and Horn, as shown in Table 8.

Table 8—Electrodes for Cold Welding. Sarazin[142]

Wall Thickness of Casting, Inch	Diameter of Electrode, Inch	Amperage
0.25	0.08–0.10	50–70
0.5–0.6	0.12	80–90
0.8–1.0	0.16	110–120
1.2 and over	0.20	140–160

Lewenz[118] believes that the electrode should be somewhat smaller for cast iron than for rolled mild steel, probably because smaller electrodes give better penetration.

Type of Joint.—Much of the success of cold welding depends on proper preparation of the joint. The common methods of studding are discussed by Holslag and, Warner[103] (Rosette method), Kochendörffer[143] (studs, anchors, straps and grooved joints), Roberts,[65] Schimpke and Horn[104] (general studding procedure), Davis[117] (studs and grooved joints), and Stieler[107] (studding and a form of slugging). According to Longo,[144] slugging is permissible for welds in compression.

Dimensions of studs are given by Sarazin[142] in Table 9.

Table 9—Stud Dimensions. Sarazin[142]

Wall Thickness of Casting, Inch	Diameter of Stud, Inch	Spacing of of Studs, Inch
0.4	0.24	0.6
1.0	0.40	0.85–1.0
2.0	0.47–0.63	1.0

Gerbeaux[50] believes that studding should not be attempted if the wall thickness is less than 0.7–0.8 inch. Lebrun[145] recommends that stud holes be drilled to different depths to give better strength, and favors the use of straps. Improper use of straps combined with poorly executed welding may, however, result in total loss of welded part.[143] The use of a steel pin threaded into both halves of a broken casting to hold them together during welding is explained by Hutchinson.[147] Several methods have been suggested to insure machinability of one side of the weld. Wedemeyer[148] fills in the bottom third of the V with lead strengthened by a stud. Namack[19] inserts a copper strip in the bottom of the V and McOscar[70] uses the same device to relieve contraction strains. A soft iron rod instead of copper is recommended by Curtayne,[149] and Wanamaker and Pennington.[150] The rod is inserted at the bottom of the V of fractured automobile engine cylinders that must be machined after welding. Curtayne also describes in detail the spotting and recess methods for fractures that need not be veed out.

Repairs.—Examples of repairs of railway equipment are given by Miller,[151] Thompson[152] and Titscher[153] (cold process). Several typical cold repairs are illustrated by Davis.[117] Lebrun[154] gives a detailed description of studding preparations for marine engine cylinders. The repair of engine cylinders is described as early as 1895.[155]

Carbon Arc Welding

The technique of carbon arc welding closely resembles that of hot metal-arc welding. The most recent detailed description of the process is given by Titscher.[156] The parts to be welded are placed in a special muffle similar to Eger's[157] surrounded by a charcoal or coke fire. The carbon electrode (positive) is raised and lowered by a pulley attachment. Using 360 to 380 amps., 33 to 40 lb. of metal are melted per hour. The proper preheating temperature is 700° C., according to Roberts,[65] not over 420° C. according to Bowers,[113] and about 500° C., according to Namack,[158] who also states that the casting should be annealed at 850° C. after welding. Besides giving complete details about the process, Namack states that graphite is better than hard carbon for electrodes, and that a flux containing equal parts of Na_2CO_3 and $NaHCO_3$ is useful. Peter[159] found that powdered glass was a good flux, and Bowers[113] recommends the addition of a little NaCl to the melt to remove small blow-holes. Davis,[117] who is apparently the only proponent of cold carbon arc welding, states that hydrated borax is occasionally used as a flux.

The filler rod for carbon arc welding must be a superior grade of cast iron, according to Davis,[117] but Roberts,[65] who recommends a filler rod with over 3% Si, and others who have written on the subject do not place any more emphasis on the filler rod for the carbon arc process than for any other process. Steel filler rods are not satisfactory according to Steel.[135] The polarity of the electrode is negative, according to Stieler,[107] who does not agree in this respect with most writers. The diameter of the carbon is $\frac{1}{2}$ to $1\frac{1}{4}$ inch, and the amperage is 300 to 600, at 50 to 60 volts. As extreme values, Unland[160] gives 100 amps. for $\frac{1}{4}$-inch electrodes, 1000 amps. for 1-inch electrodes. At 500 amps. 6 lb. per hr. is deposited, using hard, uncored, uncoated carbon.

The advantage of the cold carbon arc process, according to Davis, is that oxides, which are the cause of hard spots, are floated out of the metal. The carbon arc can be played on the work to prevent too rapid cooling, according to Lewenz,[118] and so to produce a more machinable joint than by the cold metal arc process.

Candy[161] in 1921 stated that the carbon arc was the best process for cast iron, but Lloyd and Primrose[133] state that the process carburizes the iron and causes hard spots. Owens[120] had fair success with the process. Monroe[162] has used the carbon arc for grooving out cracks in semi-steel gears prior to gas welding. The crack is slightly preheated with a gas lamp and the carbon is $\frac{5}{8}$ inch diam., 600 amps. Tedious and dangerous chipping is thus avoided.

Thermit Welding

In the Thermit process a sand mold is built around the chipped or machined fracture and the parts are preheated. Molten metal is then poured into the mold. Hulbert[163] states that thermit suitable for cast iron is plain thermit (a mixture of finely divided iron oxide and aluminum) to which 3% ferro-silicon and 20% mild steel punchings are added. The punchings serve both to lower the temperature of the molten metal and to control the chemical composition. Gerbeaux[50] also mentions large additions of ferro-silicon to thermit for cast iron and gives a sketch of a typical mold. The casting is preheated to a dull red heat and the thermit steel fuses back 2 to 3 inches into the fractured cast-iron parts. Deppeler (private communication) states that the Thermit process is used for heavy sections of ordinary, low- or high-alloy cast iron in which one dimension of the cross section is not more than eight times the other dimension. When the shrinkage of the parts to be welded is restrained, it is compensated by either springing the parts or heating a parallel member.

The Thermit weld metal is, of course, almost always stronger than base metal, depending on analysis. Lamberton[164] (1909) and Koob[165] (1914) also describe molds for thermit and burning-on. Steel[135] is of the opinion that the thermit process is not generally suited to cast-iron welding. The thermit steel is carburized and unmachinable, and has a different coefficient of expansion from cast iron. But Gjertsen[166] states that the process may be used for iron railroad castings and an anonymous writer[167] has given a number of examples of successful thermit welds in cast iron using thermit steel.

Burning-On Process

The foundry practice of burning-on is described in full by Simore[168] and three anonymous writers,[169] who give details of molds and other arrangements. The method is used to weld together broken castings, not to repair cracks. A sand mold is built round the defect in such a way that molten metal may be run against the defective part of the casting and then away to a pig bed or ladle. When fusion has occurred, as ascertained by probing with a steel rod, the overflow is stopped off.[170] Difficulties have been encountered when the casting is low in silicon or manganese, or high in sulfur. The preheating temperature is sometimes 400–450° C. List[171] describes the burning-on process in full for preheated cast-iron pipe. Lloyd and Primrose[133] have shown that the graphite surrounding the burn is not appreciably altered by burning-on because the temperature of the process is not so intense as in other processes. A brief discussion of burning-on is given in a recent text-book by McWilliam and Longmuir[172] who, however, are of two minds concerning the principles involved.

An interesting peculiarity of burn-on welds in American cast iron by British foundries was disclosed by Smith[173] in 1923. The welds invariably were riddled with blow-holes in cellular formation, each cell being $^1/_{64}$ to $^1/_4$ inch diameter, and 1 to $1^1/_2$ inches long. The base of each cell was at the actual union of the weld; the surface of the weld was solid so that the cells must have been formed after pouring was completed. No explanation was found.

Other Methods

Several writers have mentioned that the atomic hydrogen process is suitable for cast iron, and Münter[174] has briefly studied the structure of atomic hydrogen welds in gray cast iron. However, Schimpke and Horn[104] state that the process is not suited for gray cast iron but advance no reasons. The oxyhydrogen torch, according to Rosa,[175] is a prolific source of blow-holes on account of its low temperature. Cast iron cannot be resistance butt- or flash-welded to cast iron, according to Woofter,[176] but Tilemann[177] had some success in resistance butt welding malleable cast iron.

Welding Cast Iron with Non-Ferrous Filler Rods

There are two common methods of joining cast iron by means of non-ferrous filler rods. The generally accepted method of welding cast iron is with bronze, in which a bronze filler rod having a lower melting point than cast iron is deposited by means of the oxyacetylene torch. Probably at least as much cast iron is bronze-welded as fusion-welded. The second method is arc welding with non-ferrous electrodes usually having a higher melting point than cast iron. In both processes the joint is essentially different in chemical and physical properties from cast iron. Strictly speaking, therefore, neither process may be designated "welding." The first method has come to be known as bronze-welding, the second as monel welding. The first is often referred to as brazing or hard soldering.

Bronze-Welding

General articles on bronze-welding have been prepared by Endicott,[178] *Oxy-Acetylene Tips*,[179] and a French journal.[180]

Preheating.—Preheating, although not always necessary, is usually applied to assure success, according to Jones,[181] who recommends a black heat. Wikoff[26] also advises a black heat—hot enough to char a piece of newspaper, and three writers[182,183,184] in 1935 are of the same opinion. Van Alstyne states that if preheating is above 700° C., the graphite in high-carbon cast iron gives rise to a greasy effect on the scarves. However, Becker[185] and Oxy-Acetylene Tips,[179] also in 1935, recommend preheating to 900° C., about 20° C. above the melting point of bronze. The preheating may be local or general depending on the casting, according to the latter, but Becker recommends only local preheating with the torch. The Robusco[186] method also requires preheating to 900° C., which is the melting point of the bronze. This is no longer preheating in the usual sense of the word. Too low a preheating temperature is said to be a source of porosity. Monroe[162] describes the use of gas preheating torches for semi-steel gears, and Schneider[43] advises against the use of charcoal fires for preheating on account of direct action of flames. A railway welder,[328] however, prefers charcoal to coke or wood, and states that preheat temperature is usually lower than for fusion welding.

Technique.—As pointed out by the International Acetylene Association,[187] if the temperature of the base metal in bronze-welding is too low, the bronze does not spread out; if it is too high the bronze collects in little balls which are driven away by the force of the flame. The latter effect may perhaps be connected with the high vapor pressure of "bronze." The I. A. A. prepared Table 10 showing average deposition rates.

Table 10—Bronze-Welding Deposition Rates. I. A. A.[187]

Bronze Deposited— Lb. per Hr.	Acetylene Flow, Cu. Ft. per Hr.	Oxygen Flow, Cu. Ft. per Hr.
0.23 to 0.40	2.3	2.5
2.60 to 4.30	26.0	28.6
8.80 to 14.70	88.0	96.8

According to L'Air Liquide,[188] the temperature of the cast iron should not be above 775° C. nor below 650° C. Using Tobin bronze, Gerbeaux[189] has found an angle of 40° between torch and base metal most effective. To avoid overheating, the torch should be held at a smaller angle to the deposited metal than in welding, according to Trunschitz.[190] Bronze-welding of large joints should be done in several layers. Overheating causes loss of zinc and formation of gas bubbles,[191] as well as brazing effect, which, in Clark's[40] opinion, should be avoided. Becker[185] states that the bronze should be held fluid a moment or so after the V is filled to avoid porosity. The removal of graphite from the surfaces to be welded has always been considered important. Rudeloff,[192] describing Pich's process (1901), states that the cast-iron surface is decarburized by cuprous oxide mixed with borax as a flux after which the brazing metal is brought into contact with the surface without exposure to air. Kinzel and Lytle[193] recommend that the graphite be removed by scaling the surface at 900° C. After the cast iron has cooled, the bronze is said to flow readily on the scaled surface. Van Alstyne[84] states that the graphite may be re-

moved by using an oxidizing flame. Bronze-welding is difficult if the cast iron is oil-soaked, or rusty, or has been exposed to salt water and some chemicals. The "back-step" method of bronze-welding cast-iron pipe (2 to 48 inches diameter), which consists of depositing 3-inch beads so that each succeeding bead backs into the preceding, is described by Carson,[194] who also gives a table of speeds and ideal thickness of each bead. In welding cracks the welder must be cautioned against heating and thereby expanding the main section of the crack until the end of the crack is closed off with bronze. Bronze welds may be made in rainy weather and in snow storms without difficulty, according to Tupholme.[191]

Type of Flame.—A slightly oxidizing flame is usually recommended.[40,185,193,195,196,197] The ratio of oxygen to acetylene, according to Gerbeaux,[196] is 2.5 times as great as in gas welding. The excess oxygen oxidizes the graphite, but as Becker[185] points out, too much oxygen is a source of porosity. Trunschitz,[190] Tupholme,[191] and the Cast-Iron Pipe Research Association[198] (1927) recommend strictly neutral flame.

Fluxes.—There appear to be two purposes of fluxes in bronze-welding. Lüder[199] and Gerbeaux[196] consider that the flux should be oxidizing in character in order to remove graphite. Kinzel and Lytle,[193] however, believe that the flux should remove oxide films from the base metal, otherwise capillary flow of the bronze (tinning) will not occur without over-heating. Two welders[200] describe anti-oxide fluxes, and a flux containing 60% powdered brass. Weckwerth[201] recommends borax, and Othegraven[202] discusses bronze-welding fluxes in general. Blow-holes in the junction zone between cast iron and tobin bronze are attributed by Witte[112] to flux containing water. For metal difficult to tin, cast-iron flux, according to Van Alystyne,[184] is superior to brazing flux. For grown castings, *Oxy-Acetylene Tips*[179] recommends a strong oxidizing agent such as potassium chlorate, as flux. After the effervescence due to oxidation has ended, a normal gray iron surface is presented for welding.

The International Acetylene Association[187] estimates the average flux consumption at 0.025 lb. per pound of bronze deposited.

Filler Rods.—The customary filler rod for bronze-welding, according to Becker[185] and the International Acetylene Association,[187] is a brass containing 60 Cu, 40 Zn. If the zinc content is too low the hot strength is correspondingly low; if zinc is too high the deposited metal is too hard and brittle. Tin, iron, manganese, nickel[328] and silicon, about 1%, are often added to improve the flowing characteristics, decrease fume, deoxidize and increase the hardness. According to *Gas Industry*,[203] lead should always be avoided. Séférian[204] mentions a Cu-Zn-Si high-strength brass, melting point 880° C., for malleable cast iron. Several compositions suitable for enameled cast iron are given by an anonymous writer.[205]

Type of Joint.—According to Becker,[185] the V need be only wide enough so that the torch can be inserted. Gerbeaux[189] states that double V joints should be used in sections over 0.4 inch thick, but Othegraven[206] and *Oxy-Acetylene Tips*[207] found that veeing is not always necessary. The shear type of joint with 70° angle is recommended by the latter. Rockefeller,[208] who compared the collar V and shear V joints for pipe welding adopted an 80° shear V, the optimum distance of parallel portion from the top of the joint being $^3/_{32}$ inch. The Linde Air Products Co.[197] recommend that the shear step be $^1/_3$ the thickness of the pipe beneath the outside of the pipe (Fig. 1). Gavanda[209] and others have pointed out the advantage of the shear V joint. However, the shear area principle does not appear to have been adopted extensively for fillet joints. Trough-shaped scarves are said by Trunschitz[190] to avoid the surface tension effects acting to produce defective penetration in V joints. The trough or U should also be employed for cracks so as to avoid trapping air at the bottom of the deposit. Strebel[210] advocates the questionable practice of slugging brazed joints with steel wire inserts to raise the strength. A method for bronze-welding heavy sections in which the brazing metal is run into the fracture through holes drilled in the casting is described by Anders.[211] It is Van Alstyne's[184] experience that torch cut bevels are hard to tin unless they are thoroughly sand-blasted. Sand-blasted chipped bevels are superior to ground. Oil should be burnt from the surface as Ames[335] found. Brazed joints were first applied to cast-iron pipe in this country in 1921,[212] using the collar type of joint, which is seldom used at present. Hering, Outcalt and Greene[213] found that expansion joints should be inserted in brazed cast-iron pipe every 100 feet to allow for temperature changes.

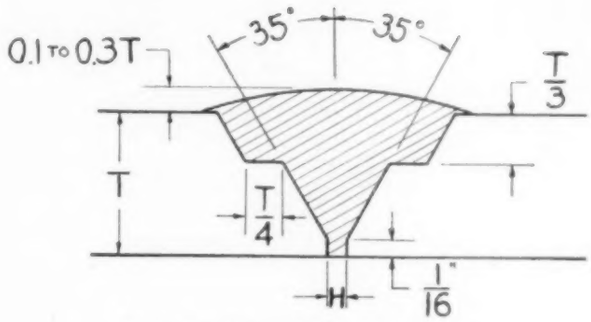

Fig. 1—Shear V Bronze-Welded Joint for Cast Iron (Linde Air Products Co.[197] 1936)
H = $^1/_{16}$ Inch after Tack Welding

Repairs.—Examples of cast iron repairing by means of bronze-welding are given by L'Air Liquide[188] (172 photographs of bronze-welded cast iron with details of gas and rod consumption, time, weight of casting, etc., in many cases), *Welding*,[214] Glatzel[215] (special bronze), French[216] and Swiss[217] welding magazines (no preheat) and by Trunschitz[218] (no preheat, cost analysis). Meslier[219] describes the bronze-welding of large gray and malleable castings, and a writer[220] in *Welding Engineer* describes the bronze-welding of a large rolling mill frame using a light preheat.

Metallurgy.—Metallurgical investigations of bronze-welding have been confined almost exclusively to the examination of the nature of the bond. As Witamwas[221] and the International Acetylene Association[187] point out, the bond is developed by intergranular penetration of brazing metal along graphite flakes or grain boundaries, by solid solution formation, and by tinning.

It is agreed that graphite, especially flake graphite, causes poor adhesion, as Lüder[199] and Gerbeaux[59,189] state. The former found that gray cast iron heated in a mixture of iron powder and boric oxide (B_2O_3) at 900° C. is rapidly decarburized and that the decarburized surface is easy to tin. Gerbeaux found that pearlite, ferrite, cementite and phosphide eutectic had no effect on the bond, but that flake graphite was detrimental. Van Alstyne[184] and Fliess[186] also observed that the finer the grain or the harder the iron the easier it is to tin. Fliess, by the way, makes a bare mention of surface tension and viscosity as important properties of brazing metal.

There is considerable evidence to show that brazing metal penetrates the cavities left by oxidized graphite flakes. Jansson[222] made this observation in 1924, and

Fliess[186,195] gives a fairly convincing demonstration of the penetration of Tobin bronze in flake cavities formed through the action of an oxidizing flux. It should be noted, however, that the extensive micrographic studies by Lytle[223] and Jackman[224] give no indication of penetration into graphite flakes. The micrographic study by Othegraven[206] of bronze welds made by a process involving a special flux clearly showed a zone 0.03 inch deep in which iron and brass had formed a layer of unnamed constituent but there was little evidence of penetration along graphite flakes.

Jackman[224] studied the diffusion of the constituents of brazing metal into a pearlitic cast iron containing steadite (composition not given). Three bronzes were used, Table 11. Specimens as brazed had a decidedly finer pearlite grain size in a zone extending 0.001 inch back of the brazing metal. Heat treatment for 6 hours at 850° C. was ineffective in developing a solid solution band of appreciable width but after 24 hours at 950° C. a zone 0.01 inch deep was developed in which the ferrite grains were dotted by some unnamed unconstituent. The heat treatment naturally altered the structure of the graphite considerably and there was undesirable dezincification.

Table 11—"Bronzes" Tested by Jackman[224]

	Cu	Zn	Sn	Fe	Ni
Bronze 1	60.0	39.25	0.75	...	...
" 2	59.0	40.3–39.0	0.5–1.5	0.2–0.5	...
" 3	57.0	39.3	...	1.5	2.2

Consequently, Jackman prepared specimens finished to a flat slightly roughened surface, etched with 10% HCl. These were coated with borax and held 20 hours at 975° C. under the three molten bronzes, no loss of zinc occurring. After the specimens had been furnace cooled, the bronze was found to have penetrated the metallic part of the cast iron along crystal boundaries, and a bronze iron alloy had diffused into the bronze. There was no penetration into graphite flakes. Specimens immersed in molten copper for 20 hours at 1150° C. contained no graphite or phosphide; the microstructure consisted of pearlite and intergranular copper. Specimens immersed in zinc for 6 hours at 550° C. and in tin for 7 hours at 425° C. showed alloy zones at the junction between cast iron and liquid metal, but there was no penetration along graphite or phosphide constituent, nor was there any detectable intergranular penetration. A solid solution zone was observed in nickel-coated specimens heat-treated at 1200° C.

Jackman concluded that the bond in bronze-welding is mainly surface alloying. There is some intergranular penetration but, since only a small fraction of the cross section of junction is grain boundary material, only a small fraction of the strength of the bronze weld can be attributed to the intergranular penetration. Jackman does not clearly show, however, that the development of a solid solution zone on both sides of the junction between bronze and iron is necessary to add to the surface adsorption which presumably accounts for "tinning."

In fact, Lüder[199] found no clear evidence for solid solution formation in joints although he believed that $FeZn_7$ may be formed. He also states that the extent of alloying of iron with brazing metal decreases as the carbon content of the iron is increased. Fliess[195] and Gavanda,[209] on the other hand, recommend heat treating the bronze-welded joint for 20 minutes at 750–800° C. to aid the formation of solid solutions, and a writer[225] in *Acetylene Journal* goes so far as to state that bronze-welding can occur by alloying even though the bronze is not melted. According to Anders,[226] there is an optimum time of heat treatment to obtain the best physical properties with optimum thickness of solid solution zone (no details). Becker[185] also recommends heat-treatment during which first the zinc then the copper diffuses into the cast iron to form a Cu-Zn-Fe solid solution of higher strength than cast iron.

In his extensive micrographic study of bronze welds (no details) in centrifugal cast (ferritic matrix) and sand cast (pearlitic matrix) pipe, Lytle[223] found no evidence of a solid solution zone upon examination at a magnification of 430 diameters. The temperature of bronze-welding was sufficient to cause the formation of sorbite for a short distance beyond the brazing metal in both cast irons. The bronze did not generally penetrate graphite flakes but at occasional points along the junction plane the bronze anchored itself in cavities. Lytle discovered that the bronze displaced the phosphide eutectic in the sand-cast pipe to a comparatively large distance (0.05 inch) back of the junction, but he offered no explanation. Furthermore, Lytle observed that a blow-hole was formed in the bronze in juxtaposition to an oxidized crack in the cast iron. The gas in the blow-hole was attributed to the oxidation of the iron. An examination of failed bronze welds showed that one failure was caused by penetration of flux into the cast iron. Another was due to the presence of 1.86% P in the cast iron. The phosphide eutectic vanished in some way during bronze-welding and left a porous region that was only partly filled by bronze.

Lytle, like Jackman,[224] showed that penetration of brazing metal in cast iron does not occur to any important extent along graphite flakes or cavities and believed that strength is due largely to adhesion of some sort. Unlike Jackman, however, Lytle placed no importance on extensive diffusion of bronze into cast iron. Other observations of the adhesion of brazing metal to cast iron have been made by Gerbeaux, who found that bronze will not grip an unmachined fracture in gray or white cast iron, but that it readily tins a machined surface. But Othegraven[206] states that machined surfaces must be roughened by means of a chisel to provide a grip for the bronze. Bardtke and Matting[21] noted the absence of hard spots in bronze-welded cast iron (60 Cu-40 Zn) and the International Acetylene Association[187] believe that bronze-welding metal has the desirable characteristic of yielding during cooling, thus minimizing shrinkage stresses.

Physical Properties

Table 12—Tensile Properties of Bronze-Welded Cast Iron

Investigator	Experimental Details	Tensile Strength, psi	Elongation
American Waterworks Assn.,[227] 1936	Bronze-welded anchor clips on cast-iron pipe 12 inches diameter, $^5/_8$ in. thick		
	Based on total throat area	46,000	
	" " area of fusion contact	20,000 to 25,000	
Ga.,[228] 1935	No details. Shear strength, 25,600 psi	35,600	17%

Table 12—(Continued)

Goryatsev,[138] 1932

Composition of Electrode	Diameter of Electrode, Inch	Amperes	Tensile Strength, psi
Copper Electrodes Containing Nickel			
2 Ni rem. Cu	0.20	200	14,100
8.5 Ni rem. Cu	"	200	6,700
16 Ni rem. Cu	"	200	9,900
Copper Electrodes Containing Aluminum or Silicon			
99.5 Cu, 0.5 Al	"	"	10,100
98.5 Cu, 1.5 Al	"	"	3,300
99 Cu, 1 Si	"	"	3,900

Maximum hardness was about 400 Brinell in the above series

Composition of Coating	Coated Copper Electrodes			
95 $CaCO_3$, 5 Al	100% Cu	0.16	200	11,200 to 17,300
85 " 15 Al	"	"	"	15,000
75 " 25 "	"	"	"	14,100 to 17,400
95 " 5 Si	"	"	"	19,100 to 21,600
85 " 15 Si	"	"	"	17,800 to 19,400
75 " 25 "	"	"	"	13,000

Brinell hardness deposited metal: 95–98.
" " junction zone: 300–450.

Investigator	Experimental Details	Tensile Strength, psi
Bardtke and Matting,[21] 1932	60 Cu, 40 Zn electrodes, no preheat. There were cracks and a white junction zone	0
Anonymous,[229] 1930	No details	50,000
Anonymous,[230] 1930	Peened "Brox" joint	53,000
Jansson,[222] 1924	1 inch sq. bars oxyacetylene butt welded with flux	18,720
Carson,[231] 1924	The average tensile strength of 6-inch cast-iron pipe welded with collar joints, Tobin bronze, was 68,333 lb. per sq. inch of bronze section. The average strength of the pipe was 22,631 psi. The bronze therefore need be only $^1/_3$ as thick as the pipe, but should be three times as wide as the pipe is thick to obtain a grip in shear.	

The welds tested by Goryatsev were made without preheat in gray cast iron (3.62 C, 0.85 Mn, 1.46 Si, 0.079 S, 0.02 P, 0.79 and 1.6 in. thick), 90° X, having a tensile strength of 17,800 psi. The plates (0.79 inch thick) were welded on a length of 12.4 inches; the total width perpendicular to the weld was 4 inches. The thicker specimens were identical, except that the width was 2 feet. The electrodes were positive, and D.C. was used. The copper-nickel electrodes generally gave porous welds. The high hardness of the junction zone in all welds should be noted.

Welds were also made with bare copper electrodes containing 2, 8.5 and 16% Ni, 0.5 and 1.5% Al, and 1% Si, as well as with bronze electrodes (0.27 in. diam.) of the compositions shown in Table 13.

Table 13—Composition of Bronze Electrodes Tested by Goryatsev and Co-Workers

Sn	Zn	Fe	Mn	P
12	2.5	...	...	0.5
13.09	0.21	...	...	1.12
1.0	3.7	1.0	0.5	...

Table 14—Cross Bending Properties of Bronze-Welded Cast Iron

Investigator: Anonymous,[232] 1936; Othegraven,[206] 1933

Experimental Details: 4-in. pipe, 72 in. long, three point load bronze-welded at center

Results: 22-inch deflection

Specimen	Section Modulus, Inch Cubed, on x or y Axis	Bent on Axis	Modulus of Rupture, psi	Deflection, In.	Time of annealing, min.	
Unwelded	x = 0.49	x-x	33,700	0.22	—	No defects
"	y = 0.37	y-y	33,300	0.28	—	" "
Unmachined; welded surfaces accessible	x = 0.49	x-x	31,400	0.17	10	Fracture near weld
" " " "	y = 0.37	y-y	31,500	0.25	20	" " "
" " " "	"	"	32,500	0.23	30	" " "
Unmachined; welded surfaces inaccessible	y = 0.37	y-y	31,000	0.22	10	Fracture in weld
Unmachined incomplete penetration	x = 0.49	x-x	22,400	0.13	20	" " "
Unmachined	y = 0.37	y-y	30,200	0.24	30	" " "
Machined V, weld smoothed off	y = 0.37	y-y	21,200	0.15	15	Fracture partly in junction
Weld smoothed off	x = 0.49	x-x	19,200	0.10	15	Partly in cast iron
Machined V, weld not smoothed off	y = 0.37	y-y	19,000	0.13	15	Poor bond
Weld not smoothed off	"	y-y	18,800	0.13	15	" "
Usual bronze weld, cast-iron flux	"	y-y	24,300	0.15	10	Fracture in cast iron
" " " " "	"	"	23,400	0.16	10	" " " "

Span = 24 in. all specimens.
Cross Section = 1.86 sq. in. all specimens.

All welds had hard spots and low strength. The tensile fractures of the joints made with bronze electrodes were brittle and occurred in the deposited metal.

Cross Bending (Table 14)

The surfaces[206] to be bronze-welded were first burned over with an excess-oxygen flame, with the exception of the last two. The fractures were heated to 700° C. in a smokeless fire and the torch applied for 10 minutes. The specimens were 1.2 × 1.6 inches, 25 inches long. With the exception of the last two specimens, special Porobronze flux and filler rods were used. The surfaces were brushed with a wire brush before welding. The welding was done at 850° C. using large amounts of flux. The weld was held fluid a short time after welding to permit release of blow-holes. Annealing was at 750 to 800° C.

Table 14—(Continued)

Investigator	Experimental Details
Greene,[233] 1931	Flexure tests of 6-inch centrifugal cast-iron pipe (composition not given) bronze-welded with Tobin or special bronze (composition not given). Span 9 1/2 feet Average of 11 tests bronze-welded 32,300 psi modulus of rupture Average of 3 tests unwelded 34,500 psi modulus of rupture Sand cast pipe (5 tests) bronze-welded 27,170 psi modulus of rupture Sand cast pipe (3 tests) unwelded 27,800 psi modulus of rupture 80° shear V joints were used Hydrostatic internal pressure tests showed no weakness at the weld.
Raymond,[234] 1931	Twelve sections of 4-inch sand cast pipe each 2 ft. 6 in. long were bronze-welded (60 Cu, 38 Zn, 2 Sn, trace Fe) with collar joints with 1/16 inch space between pipes. After being welded, the pipes were subjected to 95 psi internal pressure. No leakage developed. The pipes (32 feet long) were then centrally loaded (29 feet span), failing at 15% overload calculated on the ultimate strength of the pipe. The fractured bronze always tore away a certain amount of cast iron with it, which may be explained by penetration of bronze into cast iron, or by the ductility of bronze. Transverse beam tests of bronze-welded bars (24 inches long, 3 × 3/4 inch cross section) showed higher loads at fracture than unwelded bars.

Investigator	Pipe	Type of Joint	Modulus of Rupture, psi	Efficiency %
Rockefeller,[208] 1928	Centrifugal cast	Unwelded	34,500	100
	"	Collar	19,000	55
	"	V	28,750	83
	"	80° shear V	32,950	96
	Sand cast	Unwelded	27,170	100
	"	Collar	11,450	42
	"	V	21,500	79
	"	80° shear V	27,500	100
	Span 10 1/2 feet; 6-inch pipe			

In addition to the tests tabulated above there have been a number of hydrostatic and other tests reported, but the information has been so meager that reference to these tests serves no purpose. According to Becker,[185] bronze-welded joints should not be used above 250° C. The International Acetylene Association[187] states that the low fatigue limit of welding bronze is immaterial for cast-iron brazing. Gavanda[209] found that there were no preferential corrosion phenomena in bronze welds in cast iron. Tupholme[191] describes impact tests on three lengths of 6-inch sand-molded and centrifugal cast, cast-iron pipe with bronze-welded collar joints. The end of the sand cast pipe could be raised 3 3/4 feet from the ground and allowed to fall before fracture of the pipe occurred; the corresponding height for the centrifugal cast was 5 3/4 feet. Starke[235] mentions impact tests of bronze-welded cast-iron pipe with collar joints in which fracture occurred in cast iron on both sides of the weld, but not in the bronze itself.

Welding with Non-Ferrous Filler Metal Having a High Melting Point

Technique.—There is relatively little information available on Monel welding. Covered electrodes[236, 237, 238, 239] (reversed polarity,[117] short arc[329]) are always deposited on cold metal. Beads not longer than 2 inches are deposited and peened lightly. After every second layer the deposit is caulked, which is said to relieve welding stresses and improve toughness. The Monel is said to flow differently from any other metal and is difficult to deposit overhead. For gas welding bare rods are used with preheat and refined powdered borax.

Electrodes.—The usual high-melting-point non-ferrous electrode for cast iron is Monel metal (Wilson, Grade No. 12) coated with deoxidizer and flux. The amperage is shown in Table 15.

Table 15—Amperage for Monel Electrodes

Size of Rod, Inch	Current
3/32	80–100
1/8	100–130
5/32	175

The use of the Monel electrode (66–68 Ni, 27–30 Cu, 1.0–2.0 Mn, 1.0–3. 0 Fe, 0.09 S max.) was completely described in 1923 by Churchward.[241] The same electrode, currents and technique appear to be used now. Holslag[242] mentioned the use of a copper-nickel electrode, and Namack[158] and Lewenz[118] stated that Monel may be used as the first and last runs to provide machinability, the intermediate layers being steel. Namack also found that nickel electrodes usually cause hard spots and, if used with reversed polarity, produce porous deposits. Monel rods can also be used with refined powdered borax to produce machinable gas welds, according to the International Nickel Company (private communication, January 1937). A layer of Monel[330] or 18–8 is sometimes deposited on cast iron prior to hard-surfacing. Bronze electrodes for brazing cast iron are manufactured in this country but nothing appears to have been published about them.

Metallurgy.—According to Davis,[117] non-ferrous electrodes do not absorb carbon from cast iron but hard zones occur as with steel electrodes. Zimm[243] and *Industry & Welding*,[244] on the other hand, state that nickel or Monel metal welds in cast iron are soft because nickel decomposes carbide, and Simonis,[245] as a result of a micrographic study, found no hard zones in cold arc welds with Monel electrodes. Besides having poor wear resistance, Monel has a different coefficient of thermal expansion from cast iron, according to Stieler,[4] and should not be used for heated parts. But Pearce[12] believes that Monel is the best filler material for cast iron because the Monel accommodates itself to shrinkage stresses. In their micrographic investigation of arc welds with copper-nickel electrodes (no details) the British Engine, Boiler & Electrical Insurance Co.[82]

Table 16—Physical Properties of Monel Welds in Cast Iron

Investigator	Experimental Details	Tensile Strength, psi
Stieler,[107] 1936	70 Ni-30 Cu	14,200 to 21,300
Connor and Featonby,[246] 1935	Flux-covered Monel	27,600
	Brinell Hardness: weld metal: 404	
	" " junction: 244	
Bardtke and Matting,[21] 1932	Cold arc, Monel	10,100

Goryatsev,[210] 1932. NOTE: Experimental details are given in the section on tensile properties. The electrodes produced porous welds.

Composition of Electrode	Diameter of Electrode, In.	Amperes	Tensile Strength, psi
30 Ni, rem. Cu	0.20	200	3,000
(Constantan) 40.18 Ni, 1.26 Mn, rem. Cu	"	180	11,700 to 13,000
"	"	200	6,000 to 8,100
"	"	210	12,500 to 14,400
53.5 Ni, 44.09 Cu, 1.047 Al, 0.96 Cr	0.24	200	2,400
63.4 Ni, 33.0 Cu (Smit)	0.12	140	6,800

Hardness was 370–430 Brinell maximum in the above welds.

B. E. B. & E. I. Co.,[82] 1929 Namack,[158] 1923. NOTE: Experimental details are given in the section on tensile properties. The electrodes produced porous welds.

Experimental Details	Tensile Strength, psi
Fluxed Cu-Ni electrode; fracture partly in plate	8,800 to 11,000
68.25 Ni, 27.27 Cu, 1.65 Mn, 2.29 Fe, 0.080 Si, 0.025 S, 0.021 P	
Straight polarity, $^5/_{32}$ inch diameter cylinder iron	11,600 to 11,700
94.20 Ni, 4.62 Mn, 0.70 Fe, 0.12 Cu, 0.04 Si, 0.02 S, 0.005 P	
Reversed polarity $^5/_{32}$ inch diam. gray cast iron	8,100 to 8,700

found that the dendritic structure of the deposited metal is not broken up by the heat effect of multiple-layer welding. If the deposited metal contains small particles of graphite, it is an indication that the weld has been puddled. The minimum heat-treating temperature at which a solid solution band forms between deposited metal and cast iron is 950° C.

Examination of welds in unpreheated gray cast iron made with coated Monel electrodes by one of the authors revealed absence of hard layer, blow-holes, and cracks. Welds made under otherwise identical conditions but with coated mild steel electrodes showed all three defects.

Both electrodes gave poor deposits. The current was 150 amps. The analyses of cast iron and details of preparation are given in the Table on Physical Properties of Cast Iron Welds.

Metallurgical Factors in Fusion Welding

The metallurgical factors which have been found to affect the welding of gray cast iron are:

1. Temperature and atmosphere of welding
   (a) oxidation of iron
   (b) recovery of other elements
   (c) slags
2. Composition of casting
3. Coefficient of expansion (shrinkage) and thermal conductivity
4. Microstructural factors (see separate section).

The effects of process, such as technique, flux and filler rod, have been discussed in foregoing sections.

Oxidation of Iron.—Since the melting point of cast iron is about 200° C. below the melting point of FeO, molten cast iron at temperatures below about 1300° C. is covered with a solid oxide film. If the solid oxide is intermingled with the molten cast iron, it is difficult to remove. Early adoption of fluxes to make the slag fluid and of puddling, the end of the filler rod being held below the slag, shows that oxidation of iron was one of the first difficulties overcome in welding cast iron. Vivien[18] in 1909 was fully aware of the problem and its solution. Braune[106] believed that the molten metal was more rapidly oxidized if the cast-iron electrode is negative. The effect of oxygen in forming films and envelopes was also noted by Achenbach,[247] and Geiger[248] shows a micrograph of a drop of iron thus trapped in a cast-iron weld.

Recovery of Other Elements.—The recovery of elements in cast-iron welding has been studied mainly by Neese[248] and Schoeller.[249] Figures 2 and 3 by Neese show that the recovery of silicon and manganese varies directly as the proportion of ferrosilicon (50% Si) or ferromanganese (50% Mn) added to the weld metal. Using 0.31-

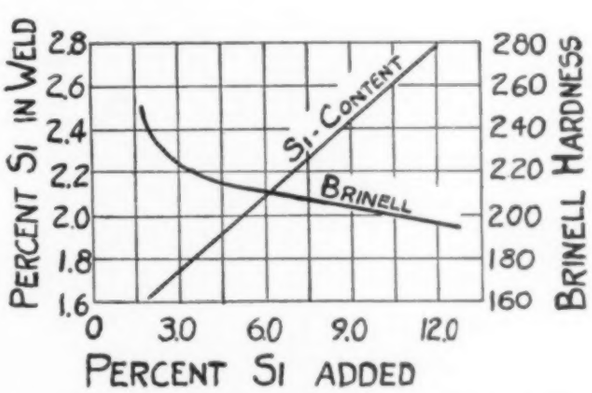

Fig. 2—Recovery of Silicon Added as 50% Ferrosilicon to Metal Arc Welds and Its Effects on Hardness (Neese[248])

Fig. 3—Recovery of Manganese Added as 50% Ferromanganese to the Weld and Its Effect on Hardness (Neese[245])

inch cast-iron electrodes, Schoeller found the losses shown in Table 17 as a result of welding.

Table 17—Loss of Elements in Arc Welding. Schoeller[249]

Element	Amount Lost (original analysis minus final analysis) 20 Volts	35 Volts
Carbon	0.35	0.70
Silicon	0.27	0.49
Manganese	0.20	0.28
Sulphur	0.005	0.13
Phosphorus	0.18	0.03

Hobrock and Walsted[250] found that the loss of silicon during oxyacetylene welding was 10%—the same loss as in cupola melting. The analyses shown in Table 18 were obtained from the original rod and from all-weld

Table 18—Loss of Elements in Gas Welding. Hobrock and Walsted[250]

	C	Si	Mn	S	P
Before Fusion	3.32	3.06	0.550	0.103	0.614
After Fusion	3.06	2.76	0.471	0.098	0.622

metal deposited in a $1^1/_2$-inch pipe, $1^1/_2$ inches long. Tibbenham[73] reports the results shown in Table 19 for

Table 19—Recovery of Elements in Gas Welding. Tibbenham[73]

	Graphite	Combined Carbon	Si	Mn	S	P
Base Metal	3.31	0.82	2.18	0.73	0.041	1.12
Welding Rod	3.33	0.96	2.19	0.73	0.039	1.13
Weld Metal	3.30	1.99	2.11	0.69	0.039	1.13

gas welding preheated gray cast iron. The weld metal was 361 Brinell, so that his rod probably contained too little silicon, although surprisingly little was lost during welding.

Overheating.—The loss of carbon and silicon in cast-iron welding has been attributed by Gerbeaux[50] and Titscher[156] to overheating in the intense welding flame or arc. Geiger[248] states that silicon protects the bath from oxidation, and Vivien[18] that silicon oxidizes before carbon. Quantitative experiments have not been made.

Gas Holes.—The oxidation of carbon during welding is often assigned as the cause of gas holes in the weld. Candy[161] in 1921 believed that the gas was CO. Geiger[248] states that the blow-holes in cold welds are partly due to CO and CO_2. Roberts[251] also gives CO_2 as the gas in blow-holes, which is trapped in the sluggish, pure iron weld metal. Pearce[12] recently pointed out that it is combined carbon that is probably responsible for blow-holes, because it is well known that graphitic cast iron is decarburized with difficulty. Pearce's explanation does not appear to distinguish between gas evolved in the solid and liquid states. It is possible that the gas holes sometimes observed are a product of a reaction during solidification of improperly protected welds. Rosa[175] (1909) attributed blow-holes to sand grains, and Dawson[252] to slag particles, but no explanation is given. Nor does Alard[253] explain his statement that moisture in cast iron contributes to blow-holes. Owens[120] performed experiments on the origin of blow-holes in preheated welds made by a carbon or metal arc, but the results were inconclusive.

Slags.—Little appears to be known about the metallurgical characteristics of cast-iron welding slags. Vivien[18] found that the silica produced in the oxidation of silicon formed ferrous silicate, and Knoch[17] observed that SiO_2 increased the viscosity of the slag. According to Namack,[158] kish (massive graphite) causes the formation of iron silicate or slag on the surface of the weld and that kish could be removed by burning over the surface of the joint with a carbon arc before welding. Silica and MnO are trapped beneath the surface of the molten bath which is the first portion of the deposited metal to solidify, Titscher[156] states.

A test for the weldability of cast iron is described by Jennings.[58] If, when a carbon arc is struck on the casting, the metal becomes fluid and spreads evenly over the base metal, good fusion can be expected. If the molten metal recedes from the base metal at the fusion line, poor fusion is indicated.

Composition of Casting.—The chemical composition of the casting to be welded is a major factor in welding. Lloyd and Primrose[133] state that low-silicon cast iron, with its small graphite flakes, is easier to weld than high-silicon because large graphite flakes do not provide a clean foundation for the weld. In Hoffmann's[254] opinion cast iron containing over 0.9 to 1.5% Mn or over 1.8% Si is difficult to weld by any process, whereas welding is facilitated if the casting contains nickel, titanium or vanadium. Connor and Featonby[246] state that phosphorus tends to segregate and is bad for welding, but manganese, on the other hand, increases hot strength and resistance to shrinkage cracks. Neese[255] is of exactly the opposite opinion, over 1% Mn causing hard spots and phosphorus being, at any rate, not a disadvantage.

According to the International Nickel Company (private communication, January 1937), puddling, preheat, cooling and types of joints are the same for low-alloy gray iron castings (2.85 C, 1.75 Si, 1.50 Ni; 3.30 C, 2.10 Si, 0.75 Ni, 0.30 Cr; 3.30 C, 1.75 Si, 1.75 Ni, 0.60 Cr; 2.70 C, 2.40 Si, 1.30 Ni, 0.40 Mo) as for unalloyed cast iron. Austenitic cast iron, as Pearce[12] remarks, has no A_1 point and has a higher resistance to scaling than gray. Resistance to oxidation restrains growth and blow-holes. Tibbenham[256] notes the formation of a brittle martensitic transition zone in welds in cast iron containing high nickel, copper and chromium. The welding of high-silicon, acid resisting castings is very difficult[257] and is discussed in the review on Special Ferrous Alloy Castings (not yet published).

Shrinkage.—The importance of the A_1 point in controlling shrinkage cracks and distortion in cast-iron welding is stressed by Pearce.[258] High silicon raises the temperature of the A_1 change to temperatures at

which inelastic accommodation to the expansion is possible. By analogy with steel a pearlitic matrix should cause more shrinkage difficulties than a ferritic, for which reason annealing prior to welding may be advantageous. Politz[69] seems to have made the only experiments on shrinkage in cast-iron welding, although Rosa[175] in 1909 was aware of the varying temperatures in weld and base metal and of the expansion at the A_1 point, and developed an approximate method for computing shrinkage. Politz measured the temperature at several points along a cast-iron bar (3.07 C, 2.04 graphite, 2.9 Si, 0.73 Mn, 0.116 P, 0.064 S) 6 inches long, 0.83 inch diameter, heated at one end for 90 seconds with an oxyacetylene torch (28 cu. ft./hr.). The results are collected in several graphs. At a distance of 0.6 inch from the heated end the temperature rose to only 700° C. Sasaki[114] found that thermal shrinkage is counterbalanced to some extent by the expansion due to graphitization.

To what extent reversed polarity and low amperage decreases shrinkage stresses in cold welds is not known, according to Namack[155] and Roberts.[251] McOscar[70] lays down the general rule that a weld metal, such as soft charcoal iron, that has the same coefficient of expansion as cast iron, will give rise to the least shrinkage stresses. He does not consider the effect of a composition gradient, however. The difficulty in welding cast iron, in Stursberg's[259] opinion, lies in irregular heat transmission due to irregular distribution of graphite flakes. Kohrs[13] attributes the difficulty to the failure of cast iron to pass through a plastic range during and after solidification.

Grown Castings.—If cast iron is held a long time under oxidizing conditions at 400 to 800° C., the metal surrounding the graphite flakes is oxidized throughout the thickness of the casting and, as a consequence, the casting increases in dimensions; that is, it grows. The strength of the casting is correspondingly decreased. Pearce,[258] Geiger,[248] Neese[255] and Namack[155] state that grown castings cannot be welded. The reason, as Neese suggests, is that the molten cast iron collects in globules which cannot be made to flow together. Raabe[260] has found that grown cast iron cannot be arc welded hot or cold, but oxyacetylene welding produces a reasonably good job. It is simply necessary to preheat the veed casting in a charcoal forge, weld and cool slowly. Knoch[17] also believes the arc is useless; in gas welding, a grown casting is quickly detected because the silicon has been oxidized to SiO_2 producing a viscous melt. This condition cannot be recognized in arc welding. For slaggy or grown castings Kesper[64] recommends oxyacetylene welding using a flux containing metallic aluminum, which by thermit reaction, absorbs the oxides. The flux is sprinkled on the red hot casting giving rise to a bright flame and fluid cast iron. Blow-holes are thus avoided. An oxidizing flux containing potassium chlorate is recommended for the same reason by *Oxy-Acetylene Tips*[179] for brazing grown castings, and Stieler[107] advocates the use of monel electrodes which penetrate the open flakes. Nyquist[110] places carbon plates along the edges of the preheated scarves of grown castings to prevent the molten metal from flowing away.

Microstructural Factors in Fusion Welding

The metallographic approach to cast-iron welding problems has been used by a large number of investigators. A general elementary discussion of the microstructures developed in cast-iron welding is given by Rollason.[261] Stieler[107] also explains a number of typical micrographs. The main problems that have engaged the attention of metallographists are: (1) hard, unmachinable zones; (2) graphite structure in weld and junction zones; (3) matrix structure in weld and junction zones; (4) heat-treatment; (5) the rôle of phosphorus.

Table 20—Temperature at Which Different Kinds of Carbon Begin to Oxidize. Sawamura

Kind of Carbon	Ash Content %	Temperature of the Beginning of Oxidation, ° C.
Wood Charcoal	1.09	390
Temper Carbon from Malleable Cast Iron	0.08	610
Graphite from Sand Casting	0.10	650
Kish	0.05	740
Natural Graphite (Ceylon)	0.10	830

Hard Spots.—There is unanimous agreement that the hard, unmachinable zones generally found in cold welds and in improperly executed hot welds consist of white or nearly white cast iron. Invariably the hard zones contain not merely excess cementite but ledeburitic or eutectic cementite, suggesting that the composition or rate of cooling has been unfavorable to the precipitation of graphite during or immediately subsequent to solidification. It should be remembered that the nature of the cementite-graphite relationship is not clearly known at the present time.

Beyond agreement as to the constitution of hard spots there is no agreement whatever among investigators concerning the cause of the phenomenon. The causes discovered by various investigators are: (1) Insufficient preheat;[32,59] (2) rapid cooling;[37] (3) local loss of carbon and silicon during welding;[37] (4) carburizing flame;[69] (5) oxidizing flame;[37] (6) neutral flame;[262] (7) blue cone of flame held too close to weld metal;[50,263] (8) incorrect flux;[15,37,59] (9) too thin a rod or too small a torch;[69,243] (10) sand in filler rod;[37,50] (11) FeO in filler rod;[37,72] (12) migration and decomposition of phosphide eutectic.[82]

Dealing with these causes in order, it is certain that insufficient preheat and rapid cooling may both cause hard spots. Quantitative information on these factors is not available. Local loss of carbon and silicon, according to Baillon, may also be a factor. He found that the proportion of hard spots decreased as the rate of flow of gas in the torch was increased from 50 to 200 liters /mm./min. Baillon considered that this observation absolutely refuted the belief that simple loss of carbon and silicon was a cause. As the power of the torch was increased, the weld metal became more homogeneous on account of the greater amount of heat supplied to it.

Atkins[264] also states that hard spots, which are found surrounding blow-holes, are caused by oxidation of carbon from the metal in the vicinity of the gases in the blow-hole. The resulting iron is relatively low in carbon and tends to solidify white. According to Jennings,[68] the presence of blow-holes in conjunction with hard layer is explained by volatilization of carbon and silicon, and by the effect of heat on the layer of graphite, dirt and slag. No experimental results are given.

Concerning (4–6), Politz found that an excess acetylene flame increased the carbon content of weld metal from 3.32 to 4.02%, but strictly speaking, he did not imply a greater sensitivity to hard spots. Ewden,[262] however, found that gas welds in cast iron containing 3.55 C, 3.06 graphite, 0.54 Mn, 1.8 Si, 83–84 Rock-

well *B* (156–159 Brinell), had a hardness of *B* 100–101 (241 Brinell) using a neutral flame, but only *B* 95–96 (207–212 Brinell) with excess acetylene, and *B* 97 (223 Brinell) with slight excess of oxygen. The effect of holding the blue cone of the flame too close to the weld metal was given by Granjon[263] (1911) as a cause of hard spots. Later experiments on this subject are discussed in the section on Fluxes in Gas Welding.

Hard spots, according to Zimm,[243] are caused by suddenly stopping the welding and moving away the arc or torch. Politz[69] attributed hard spots to the numerous reversals of temperature involved in using too thin a rod and too weak a torch. Simultaneously there was excessive oxidation. The action of sand and occluded oxides having a high melting point (Wuttke[34]) in causing hard spots is not clearly explained. Iron oxide in the filler rod is said to be reduced by the flame and subsequently carburized to form a hard grain. Phosphide eutectic on account of its low melting point has been observed to migrate to the junction zone during welding. There it is partly transformed to ledeburite. The majority of investigators believe, however, that phosphorus is unimportant in cast-iron welding.

The micrographic study of cold welds made by Obrebski[265] clearly showed that, although the annealing effect of multiple layers is evident on the mild steel, it has no effect on the hard layer. The original cast iron had a hardness of 189 Brinell (2 mm. ball), the mild steel weld metal 173 to 282, the hard zone 404 Brinell. According to Afanasiev[266] and co-workers, the graphite in gray cast iron dissolves only at temperatures close to the melting point. Therefore, hard zones are found only in the immediate vicinity of the weld, in contrast to the broad zones observed in welds in malleable cast iron.

To prevent or remove hard spots, all are agreed that annealing or very slow cooling is infallibly effective.

Graphite Structure.—An examination of the microstructure of all welds in cast iron, with the possible exception of burn-on welds, shows that the graphite structure in the weld is finer than in base metal. Pearce[12] states that flake graphite is never found in cast iron weld metal even with preheating. The fine weld graphite is superior to the flake variety from the standpoint of physical properties and owes its state of subdivision to cooling rate and probably also to agitation during cooling. Weiss[267] presents several micrographs to show the uniform distribution of graphite in oxyacetylene weld metal. The best micrographic study of graphite distribution has been made by Lytle[223] whose micrographs of oxyacetylene welds in sand-cast (flake structure, pearlitic matrix) and centrifugal-cast (eutectic graphite, ferritic matrix) iron pipe. There was not the slightest variation in size and distribution of graphite in base metal up to the scarf of the weld in either cast iron. The graphite in the weld was in the form of minute flakes. Lytle does not report the chemical composition of the cast irons and filler rods, but both contained considerable phosphorus and neither appeared unusual in microstructure He also fails to state the details of welding technique, which, in the light of the following paragraph, may have affected his results.

In contrast to Pearce and Lytle, several investigators[73, 82,133,268,269] report that it is common to observe a thin layer of altered "grown" graphite in the junction zone at the boundary to which weld metal had penetrated, which seriously lowers the transverse strength, Lloyd and Primrose report this zone in cold welds using pure iron electrodes, the British Engine Boiler and Insurance Company consider the zone a characteristic of gas and metal arc welds, and Tibbenham, who plotted the percentage of graphitic carbon *vs.* the distance from the weld in unpreheated welds, showed that there was a great increase in graphite up to $^{7}/_{8}$ inch back of the weld. An explanation of the zone of piled-up graphite was given by Alard[253] who believed that the graphite flakes on the scarves of the weld are not easily dissolved by molten metal, and constitute a junction line exposed to oxidation. Namack[158] and Bougaev[270] who noted the extreme difficulty of welding cast iron having a coarse, open structure, and the B.E.B. & E.I. Co.[82] appear to agree with Alard.

Jennings[58] states that the ashy residue can be eliminated from the weld by using covered electrodes, the slag of which brings the residue to the surface of the molten pool. On the other hand, Everest[268] believes that graphite accumulates in the junction zone as a result of decomposition of pearlite under the influence of the temperature of welding.

It is difficult to decide from the micrographic evidence of the "graphite" layer advanced by the investigators whether the layer contains graphite or slag or is simply void. That the accumulation of graphite is a difficulty is shown by the precaution often adopted of burning over the scarves before welding gray cast iron. However, the graphite layer is in no sense an unavoidable defect in cast-iron welds.

The region of union in a burn-on weld examined by Bolton[70] (see section on Physical Properties: Tensile Properties) contained coarse graphite flakes similar to the original casting which had not gone into solution although they were thinner than in the original. The coarse flakes could easily be identified among the clusters of fine graphite characteristic of the rapidly-cooled added metal. There was no trace of slag or oxide along the joint.

It seems appropriate at this point to mention two theories which have been advanced for the adhesion of weld metal to cast iron. It is the opinion of Lloyd and Primrose[133] that welds made with pure iron electrodes have perfect adhesion to cast iron owing to the high adsorptive capacity of pure iron for carbon. The grip of burn-on welds, according to the recent text-book of McWilliam and Longmuir,[172] is due solely to contraction of surrounding metal, although elsewhere the authors concede the importance of fusion.

Matrix Structure.—The structure of the matrix in cast-iron welds and junction zones is relatively unimportant. In other words, if the rate of cooling is sufficiently rapid to retain martensite, another more dangerous phenomenon, hard spots, will usually accompany it. Besides, cast iron is fundamentally brittle, and aside from machinability, no property is materially damaged by the presence of relatively brittle matrix constituents. Consequently, there has been little investigation of the matrix structure of cast-iron welds. Usually the weld metal has a finer grain than base metal, as Lytle[223] found. The dark-etching band in cast-iron welds indicates the presence of sorbite, as the B.E.B. & E.I. Co.[82] and Obrebski[265] pointed out. Ross[269] states that weld metal in cast iron deposited with a 4-gage New Era electrode (300 amps.) has a martensitic structure. The effect of rapid cooling in retaining pearlite in, and strengthening, high-silicon welds has been noted by Carnevali[271] and discussed in the section on Tensile Properties of Fusion Welds. In general, as Lytle[223] found, the matrix in pearlitic cast-iron welds behaves the same as in welds in high-carbon steels. There is usually some reversion of the pearlite in the junction zone to finer pearlite and sorbite.

Heat-Treatment.—It is common practice to anneal some cast-iron welds after welding for three reasons:

(1) To remove shrinkage stresses, (2) to break down hard spots, (3) to decrease carbon gradient. The British Engine, Boiler and Electrical Insurance Co.[82] found that heat-treatment at 850° C. for 15 minutes broke down hard spots but oxidized the remainder of the metal. Heat-treatment at 720° C. for 15 minutes has no effect on machinability and microstructure. Hobrock and Walsted,[250] whose results are discussed in full in the section on Physical Properties, found that heat-treatment at 760° C. followed by water quenching improved the strength of gas welds beyond that of unwelded cast iron. The heat-treatment was said to decrease the shearing stresses at the weld caused by differences in coefficient of expansion between weld metal and base metal, by decreasing the carbon gradient. The hypothesis is that rapid cooling evokes shrinkage stresses if weld and base metals have different compositions, and that heat treatment at 760 ° C. equalized the differences in chemical composition. Heat-treatment at 650 or 870° C. was valueless.

The Rôle of Phosphorus.—Phosphorus, occurring as it does in cast iron in excess of perhaps 0.05% P, as a ternary eutectic, m.p. 950° C., might be expected to play an important part in the microstructure. Actually, however, very few investigators have found that changes in the phosphide eutectic as a consequence of welding are of any consequence. Lytle,[223] for example found that in gas welds in pearlitic sand-cast pipe there was no change in the phosphide eutectic up to the junction with weld metal. In gas welded centrifugal cast pipe having initially a ferritic structure with eutectic graphite in which massive phosphide was embedded, the massive phosphide was found to have been transformed to typical, dotted phosphide eutectic in the junction zones.

In Lytle's opinion the massive phosphide represented eutectic of iron carbide, iron phosphide, and iron which had not developed a eutectic pattern during the rapid cooling of the pipe subsequent to centrifugal casting. When tempered by the welding heat, the unstable constituent assumed its eutectic structure and gave up part of its cementite to form pearlite which was observed surrounding the heat-affected phosphide. The B.E.B & E.I. Co.,[82] as mentioned in the section on "Hard Spots," also used the hypothesis of decomposition of phosphide constituent during welding as an explanation of the carburization of adjacent iron to form ledeburite and, consequently, hard spots. It remains to be demonstrated that the carbon involved comes from the phosphide rather than from graphite. In fact, Bolton[170] found that the phosphide eutectic in the junction zone of a burn-on weld contained bands of free cementite, which he ascribed to absorption of carbon by phosphide at welding temperature, and its retention in combined form during subsequent rapid cooling. It is certain that the change of massive phosphide to eutectic must involve complete melting of the phosphide constituent during welding. Primrose[133] has, in fact, reported the migration of phosphide eutectic to preferred regions in the junction zone, but he offers no explanation and little evidence.

Physical Properties of Fusion Welds

The physical properties of welds in gray cast iron have been determined mainly in tensile and bend-deflection tests. The hardness test has, of course, been used to a considerable extent, and something is known about impact and fatigue resistance. Nothing appears to be known, however, about the following properties: compression (aside from hardness tests), torsion, wear, corrosion and high- and low-temperature strength. The statement made in 1933 at the A. S. T. M. Symposium on Cast Iron[272] that properly made welds in gray cast iron have practically the same strength as base metal appears to be correct. In the following summary the published values of each physical property of each type of welding are grouped together.

Only the French Institute of Gas Welding[273] have formulated standard tests for welds in cast iron. Two pieces $3^1/_8$ x $2^1/_{16}$ x 0.4 inch thick are butt welded, 90° V, using the flux recommended by the manufacturer of the filler rod. Only two-thirds of the depth of the V is filled. The torch must pass 1000 liters (35 cubic feet) per hour, approximately. The torch must be adjusted perfectly neutral and the tip of the cone maintained 0.2 inch from fused metal. The welded specimen is fractured longitudinally and a piece of transparent cross-section paper (1 cm. squares) is used to estimate the percentage of blow-holes. Absolutely no hard spots may be visible; they are detected by their white color. The entire surface must be soft to the file. A Brinell hardness test 10 mm. ball, 3000 kg. load is made on the filed surface. The specifications promulgated by the German Welding Committee[274] in 1927 also required the weld zone to be soft to the file. The weld must have a dense structure and there must be no spatter. *Foundry*[274] recommended a vise-break test for cast-iron welds, the fracture showing freedom from slag and blow-holes.

The Oxyacetylene Committee[275] of the International Acetylene Association also recommend a vise-break test on butt welds 2 in. long, $^1/_2$ in. thick. which must fracture entirely in cast iron. In their nick-break test of butt-welded specimens $^1/_2$ in. thick, an applicant is disqualified if his coupon shows a defect having a dimension over $^1/_{16}$ inch, or if there are more than 10 defects per square inch.

Namack's Procedure.—The plates were 1 inch thick, 3 inches wide, sand cast in a flat mold; the weld was 6 inches long, 90° V machined. The plates were clamped and tacked before welding with straight polarity, 150 amps., 20 volts. The majority of the specimens failed partly in weld, partly in base metal. Full details of time required and metal used in welding are given. Arc length was $^1/_8$ inch. The specimens were turned to $^1/_2$ inch diameter, $2^1/_2$ inches parallel portion, $^1/_2$ inch fillets, threaded ends. The tensile strengths are maximum and minimum reported on 1 to 9 specimens. Detailed remarks concerning the behavior of the welds in sawing and turning are given but the results are not in any sense quantitative. There is no close relation between difficulty of machining and tensile strength. Namack concluded that electrodes 1, 4, 5 and 9 gave the best result with the cylinder iron and electrodes 2 and 5 with gray cast iron. It is apparent, however, that welds made by Method No. 2 were vastly superior to those made by Method No. 1.

Table 21—Shore Hardness as Affected by Technique (Namack[158])

	Straight Polarity	Reversed Polarity
Slow Welding	33 Shore	23 Shore
Fast Welding	40 Shore	36 Shore

Tensile Properties—Burn-On.—The tensile strength of a burn-on weld in a gray casting $1^1/_2$ in. thick, with a circular hole 3 in. diam. filled by burning-on is given by Bolton.[170] The weld specimen contained original metal, junction zone and new metal (Table 24).

Table 22—Tensile Properties—Gas Welds

Investigator	Experimental Details		Tensile Strength, psi Unwelded	Welded
Reininger,[14] 1936	3.2 C, 0.92 combined C, 2.02 Si, 0.78 Mn, 0.2 P, 0.1 S in weld, junction zones and base metal (no other details)		40,000–40,400 (210–225 Brinell)	47,700–48,500 (230–260 Brinell)
Tibbenham,[73] 1934	Same welding rod for both	Preheated	....	21,500
		Not Preheated	....	17,600
Bardtke & Matting,[21] 1932	Preheated; cast iron rod		....	20,200 to 20,800
Anon,[277] 1931	Unmachined	Welder No. 1	16,850	4,690 to 17,600
	"	" No. 2		12,100 to 17,700
	Machined	Welder No. 1	17,025	8,940 to 14,775
	"	" No. 2		12,725 to 17,025
	Averages of 7 specimens each, using different cast-iron rods (no details). 45° V. Fractures in weld			
Wyss,[67] 1930	Flux was a water solution of alkalies; unwelded	0.39 in. thick	25,000	25,500 to 30,800 (6 specimens)
	Cast iron = 225–239 Brinell	0.30 in. thick	23,800	38,000
	Welded = 254–270 Brinell	0.28 in. thick		34,600
	Rod contained 3.1% Si	All-weld-metal		40,200
	Weld was pearlitic and had finer graphite than base metal.	All specimens had 0% elongation		
Politz,[69] 1925		Brinell Hardness: Junction Zone / Weld Metal		
	Usual cold weld	156 / 170	30,700	14,800 (48.0)
	Usual hot weld	212 / 131		22,500 (73.0)
	Excess oxygen	166 / 163		7,000 (22.5)
	Excess acetylene	126 / 241		14,300 (46.6)
	Overheated, burnt weld	131 / 105		5,400 (17.6)
	Cold weld; 0.2% FeO in flux	166 / 163		11,000 (35.7)
	Cold weld; 1.5% FeO in flux	187 / 153		14,500 (47.0)
	Each figure is the average of two tests; size of specimen not given; all specimens broke in weld; figures in parentheses are % unwelded; Brinell determined with 10 mm. ball, 3000 kg. load, 30 sec. Unwelded cast iron analyzed 3.32 C, 2.72 graphite, 2.70 Si, 0.56 Mn, 0.126 S, 0.492 P, 165 Brinell. 60° double V welds, 1 in. plate, 8 in. long, 4 in. wide. Only one specimen in each condition was tested.			
Carnevali,[271] 1911	1 3/8 inch diam. rapidly air cooled after welding		21,700	23,100
	1 3/8 inch diam. slowly air cooled after welding			23,800
	1 3/8 inch diam. 30 minutes at 850–900° C., slow cool			21,100
	1 inch. diam. slowly cooled after welding			23,500
	Castings analyzed 2.94 C, 2.61 graphite, 2.42 Si, 0.87 Mn, 1.47 P, 0.09 S, 166 Brinell. Filler rod had same analysis, except 4.5% Si.			
	Maximum Brinell, air cooled = 227			
	" " heat-treated = 209			

Table 23—Tensile Properties—Metal Arc Welds (Hot)

Investigator	Experimental Details	Tensile Strength, psi Unwelded	Welded
Stieler,[107] 1936	Cast-iron electrode		31,500 to 43,000
Stieler,[4] 1934	Cast-iron electrode		26,000 to 31,500
Bardtke & Matting,[21] 1932	3.35 C, 2% Si casting; cast-iron electrode		19,700
E. M. F.,[217] 1931	Preheated 780° C.; gray cast-iron electrode		21,300
Sarazin,[142] 1930	Cross section = 0.26 sq. in. Using the Frémont specimen (0.04 sq. in.), the shear strength unwelded was 22,800 to 25,600, welded was 25,400 psi.	18,500 to 22,800	20,400

Cross Bending Properties.—It is generally realized that the modulus of rupture and deflection at fracture in a transverse beam test are rather more trustworthy indices of the strength and ductility of cast iron than the tensile properties. The type of grips employed in the tensile test exerts a large effect on the tensile strength of cast iron. Furthermore, the beam test gives a definite indication of ductility.

Table 24—Tensile Properties of a Burn-On Weld. Bolton[170]

C	Si	Mn	P	S	Specimen	Tensile Strength, psi
3.01	1.78	0.51	0.825	0.122	Unwelded	21,300
3.25	2.79	0.53	1.180	0.095	New Metal	36,000
..	..	..	...	...	Burn-On	25,900

The specimens[307] were 1 1/4-inch square bars, flat cast, machined to 1 inch square, 14 inches long. Analysis: 3.20 C, 2.57 Si, 0.50 Mn, 0.734 P, 0.097 S. Each bar was broken in the transverse beam testing machine, the fractured halves welded together, X joint, with high quality cast-iron rod (preheat not mentioned) containing 3.32 C, 3.06 Si, 0.550 Mn, 0.614 P, 0.103 S. Load was applied at the center, 12 inches span; the face of

Table 25—Tensile Properties—Metal Arc Welds (Cold)

Investigator	Experimental Details	Tensile Strength, psi Base Metal	Welded
Connor and Featonby,[246] 1935	Flux covered, mild steel electrode; double V weld Brinell in weld metal = 331 Brinell in junction zone = 650		8,600
Namack,[188] 1923	Electrode 1 gray cast iron Method 1	16,850 to 22,400	3,900 to 11,800
	Electrode 2 gray cast iron Method 1		6,020 to 10,900
	Electrode 2 gray cast iron Method 2		10,600 to 17,700
	Electrode 5 gray cast iron Method 1		7,710 to 14,810
	Electrode 9 gray cast iron Method 1		6,000 to 11,300
	Electrode 1 cylinder iron Method 1	38,100 to 38,900	4,800 to 12,600
	Electrode 1 cylinder iron Method 2		16,100 to 27,800
	Electrode 2 cylinder iron Method 1		14,150
	Electrode 4 cylinder iron Method 1		5,900 to 13,400
	Electrode 5 cylinder iron Method 1		7,200 to 12,900
	Electrode 9 cylinder iron Method 1		11,200 to 20,100
	Electrode 9 cylinder iron Method 2		17,450 to 33,360
	Method No. 1: Electrode deposited in a single spiral layer. Method No. 2: Straight layers covering sides of groove followed by straight layers to fill in center.		

Analysis of base metal and electrodes

	C	Graphite	Mn	Si	S	P	Cu	Ni
Cylinder iron	3.02	2.52	0.88	1.90	0.102	0.730	...	...
Gray cast iron	3.12	2.96	0.55	2.28	0.104	0.865	...	...
Electrode 1	0.100		0.466	0.040	0.025	0.039	0.056	trace bare, 5/32 in.
Electrode 2	0.010		0.012	trace	0.037	0.021	0.062	trace bare, 5/32 in.
Electrode 4	0.010		0.010	trace	0.040	0.019	0.032	trace coated, 5/32 in.
Electrode 5	0.100		0.429	0.050	0.028	0.039	0.082	trace coated, 1/8 in.
Electrode 9	0.080		0.306	0.050	0.032	0.022	0.076	trace composite, 5/32 in.

Table 26—Cross Bending Properties—Gas Welds

Investigator	Experimental Details	Brinell Unwelded	Brinell Welded	Modulus of Rupture, psi Unwelded	Modulus of Rupture, psi Welded	Deflection at Fracture, Inch Unwelded	Deflection at Fracture, Inch Welded
Türcke,[105] 1932 (Hoffmann 1929)	Ordinary cast iron	140–180	170–210	41,000	36,000	0.24–0.38	0.24
	Machinery cast iron, Grade I	140–180	140–210	43,000	44,000	0.32–0.43	0.28
	Machinery cast iron, Grade II	170–190	190–210	53,000	44,000	0.32–0.47	0.37
	Special cast iron	180–200	210–260	59,000	59,000	0.39–0.51	0.51
	Perlit cast iron	250–300	330–350	73,000	71,000	0.71	0.75
	High silicon filler rods were used with preheating.						

Analysis	C	Si	Mn	P	S
Ordinary	3.3–3.8	1.8–2.8	0.5–0.8	0.5–1.2	0.06–0.10
Machinery I	3.2–3.5	1.8–2.8	0.6–0.8	0.8–1.2	0.08–0.10
Machinery II	2.8–3.5	1.0–2.0	0.6–1.0	0.5	0.06–0.10
Special	2.8–4.0	1.0–2.8	0.6–1.0	0.1–0.8	0.04–0.08
Perlit (2.4% graphite)	3.3	0.9	1.0	0.1–0.4	0.1

Greene,[223] 1931	Flexure Tests of Alloy Cast-Iron Pipe (0.5 Ni, 0.3 Cr).	Modulus of Rupture, psi Threaded and Coupled	Modulus of Rupture, psi Welded	Deflection at Fracture, Inch Threaded and Coupled	Deflection at Fracture, Inch Welded
	Pipe Size 2	10,000	40,500	3.12	10.25
	Pipe Size 2 1/2	16,900	33,600	4.00	8.25
	Pipe Size 3	29,700	41,000	5.87	8.87
	Pipe Size 4	22,200	31,600	3.18	4.06
	Pipe Size 5	19,000	29,400	2.50	3.53
	Pipe Size 6	15,200	26,200	2.13	2.50
	Four 5-feet lengths welded or coupled into 20 feet lengths tested on 19 feet span with load at center joint. Welded with rod of same composition as pipe, slight preheat. The modulus of rupture is calculated on nominal pipe dimensions.				

Wyss,[67] 1930	(See Tensile Properties of Gas Welds)	Modulus of Rupture, psi Unwelded	Modulus of Rupture, psi Welded
	0.39 inch thick	50,000	56,700 to 60,000
	0.295 inch thick	51,000	49,500 to 63,000
	All-weld-metal		83,500

the weld was in tension except in the last series, which were tested on the side. All but two specimens of Series 1 broke outside the weld. Some bars of Series 2 had cracks before testing, and some bars of Series 3 had blow-holes in the weld. All but one bar of Series 4 broke in the weld. The specimens of Series 5 did not break entirely apart but merely cracked through and did not separate.

Two bars[137] $1^1/_8$ inch square, 4 inches long were welded (electrode negative) 90° V. Beams $^3/_4$ inch square by 8 inches long were cut and tested on 6 inches span, the face of the weld being in tension. Fractures showed no blow-holes.

Impact.—Zimmermann[278] determined the impact value of metal arc welds in gray cast iron (2.9 C, 1.7 Si, 0.4 Mn, 0.08 P, 0.03 S) welded with 0.24–0.32-inch electrodes

Table 26 (Continued)

Hobrock and Walsted,[250] 1928

	No. of Bars Averaged	Breaking Load, Lb. Unwelded	Welded	Change	Deflection at Fracture, Inch Unwelded	Welded	Change
1. Air cooled and machined	27	2108	1964	−144	0.155	0.116	−0.039
2. Water quenched immediately after welding, ground to size	15	2086	602	−1484	0.143	0.031	−0.112
3. Air cooled, machined, water quenched from 65° C.	10	2070	1813	−257	0.143	0.107	−0.036
4. Air cooled, machined, water quenched from 760° C.	10	2263	2450	+187	0.152	0.156	+0.004
5. Air cooled, machined, water quenched from 870° C.	10	2178	515	−1663	0.146	0.025	−0.121
6. Air cooled, machined, tested on side	11	2157	1995	−162	0.148	0.109	−0.039

Table 27—Cross Bending Properties—Metal Arc Welds (Hot)

Türcke,[105] 1932 (Hoffmann 1929)

	Brinell Unwelded	Brinell Welded	Modulus of Rupture, psi Unwelded	Modulus of Rupture, psi Welded	Deflection at Fracture, Inch Unwelded	Deflection at Fracture, Inch Welded
Ordinary cast iron	140–180	150–200	41,000	41,000	0.24–0.39	0.20–0.55
Machinery cast iron, Gr. I	140–180	150–200	43,000	43,000	0.32–0.43	0.47
Machinery cast iron, Gr. II	170–190	210–250	53,000	54,000	0.32–0.47	0.51
Special cast iron	180–200	240–290	59,000	58,000	0.39–0.51	0.43
Perlite cast iron	250–300	350	73,000	78,000	0.71	0.67
(For details see Tensile Tests—Gas Welds)						
Neese,[248] 1921 — Average of 8 specimens: 40 × 1.2 × 1.2 inches cast-iron electrode			43,500	42,500	0.78	0.76

Table 28—Cross Bending Properties—Metal Arc Welds (Cold)

Türcke,[105] 1932 (Hoffmann 1929)

	Brinell Unwelded	Brinell Welded	Modulus of Rupture, psi Unwelded	Modulus of Rupture, psi Welded	Deflection at Fracture, Inch Unwelded	Deflection at Fracture, Inch Welded
Ordinary cast iron	140–180	350–370	41,000	15,000	0.24–0.39	0.12
Machinery cast iron, Gr. I	140–180	370	43,000	13,500	0.32–0.43	0.20
Machinery cast iron, Gr. II	170–190	310–340	53,000	8,500	0.32–0.47	0.20
Special cast iron	180–200	310–380	59,000	36,000	0.39–0.51	0.35
Perlite cast iron	250–300	370	73,000	55,000	0.71	0.43
(For details see Tensile Tests—Gas Welds)						

Shun-ichi Satoh,[137] 1930

	Modulus of Elasticity, psi Unwelded	Modulus of Elasticity, psi Welded	Modulus of Rupture, psi Unwelded	Modulus of Rupture, psi Welded
(4C) Wrought-iron electrode with coating containing 60% graphite, 40% carborundum plus 1% barium carbonate	12.0×10^6	12.0×10^6	50,000	59,000
(5C) Wrought-iron electrode with coating containing 50% graphite, 50% carborundum plus 1% barium carbonate		12.0×10^6		55,000
(6C) Wrought-iron electrode with coating containing 40% graphite, 60% carborundum plus 1% barium carbonate		11.2×10^6		43,000

Analysis	C	Combined Carbon	Si	Mn	P	S
Cast Iron	3.413	0.724	1.905	0.286	0.251	0.04
4C	3.573	1.058	3.014	trace	trace	0.02
5C	3.906	0.504	4.317	trace	trace	0.02
6C	3.438	0.036	5.719	trace	0.202	0.02

Table 28 (Continued)

	Studded Cast-Iron Welds Made with Thin Mild Steel Electrodes. No preheat	Dimensions, Inch Length	Thickness	Rupture Load, Lb. Unwelded	Welded	Deflection, Inch Unwelded	Welded
Lebrun,[148] 1925	Specimen placed flat	7.6	2.3	21,300		0.35	
	Specimen placed flat	7.6	2.1	22,300		0.95	
	Specimen placed flat	7.6	2.2		11,500		0.55
	Specimen placed flat	7.6	2.4		12,700		0.87
	Specimen placed flat	7.6	2.2		11,100		0.22
	Specimen placed flat	7.6	2.1		9,700		0.24
	Specimen placed on edge	2.7	2.2	10,400		0.33	
	Specimen placed on edge	2.7	2.2	8,800		0.32	
	Specimen placed on edge	2.7	2.3		5,100		0.20
	Specimen placed on edge	2.7	2.2		4,800		0.14
	Specimen placed on edge	2.7	2.2		5,500		0.18
	Welded specimens always broke through the holes drilled for the threaded studs.						

		Modulus of Rupture, psi Unwelded	Welded		
Neese, [248] 1921	Average of 8 specimens 40 × 1.2 × 1.2 inches	44,500	20,000	0.48	0.24
	Average of 5 specimens 24 × 1.2 inches diam.	43,000	27,300	0.38	0.26
	Gray cast iron cold welded with mild steel electrodes.				

(2.8 C, 2.65 Si, 0.6 Mn, 0.07 P, 0.05 S, 0.04 Cu) 60° X, plates 31½ inches long, 3.2 inches total width of welded specimen perpendicular to the weld, 0.59 and 0.79 inches thick, 0.12 inch root spacing, preheated to 700° C., and cooled in sand. The impact specimen was 0.39 x 0.39 x 2.17 inches long, 0.12-inch notch, 0.08 inch diam. The results are shown in Fig. 4. The welds had 10% lower impact value than unwelded specimens throughout the range −195 to 200° C. Annealing at 900° C. for 3 hours raised impact value 5 to 7% above that for unannealed welds. X-ray examination showed that the annealing had partly eliminated internal stresses.

Carnevali[271] using the base metal and filler rod described in the section on Tensile Properties, found that Charpy specimens 1.2 x 1.2 x 6.3 inches, 4.7 inches span, machined from oxyacetylene welded bars 1.4 inches square had the same impact value (96 ft.-lb.) as unwelded specimens. Cooling rapidly after welding or annealing 15 minutes at 800° C., slow cool, had no effect on impact value.

Marmier[279] placed unwelded and oxyacetylene welded cast-iron bars (dimensions and welding details not given; the cast-iron filler rod contained 4 to 5% Si) on supports 6.3 inches apart and struck them with a 27 lb. weight, starting from a height of 13 inches and increasing in steps of 2 inches after each blow. The welded specimen fractured at the third blow; the unwelded at the fifth.

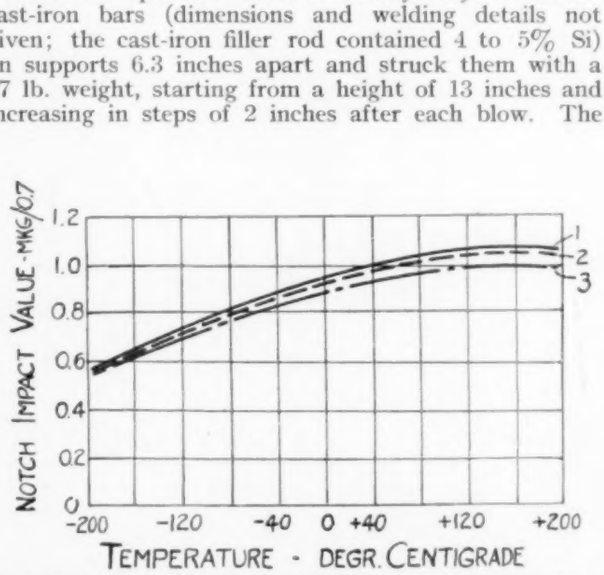

Fig. 4—Notch Impact Value (German Standard Specimen, DVM Round Notch, Values in Meter Kilograms per 0.7 Sq. Cm.) of Hot Metal Arc Welds in Gray Cast Iron. (1) Unwelded, (2) Welded and Annealed at 900° C., (3) As-Welded. (Zimmerman,[278] 1934)

Fatigue.—The rotating-bend fatigue limit of cast iron (3.12% total C, 2.34% graphite, 2.65 Si, 1.05 Mn, 0.37 P, 0.027 S, 0.06 Cu) with and without the cast skin, which had a very fine graphite eutectic structure, has been investigated by Bartels[280] using short-cycle, as well as the usual Wöhler methods. The ends of 0.79-inch bars were turned to 45° cones and cold welded by gas with a cast-iron rod (3.4 C, 3.15 Si, 0.9 Mn, 0.7 P, trace S) using a flux. The welds contained 3.21% total C, 2.18% graphite, 2.68 Si, 0.87 Mn, 0.56 P, 0.049 S, 0.12 Cu. The specimens with skin were vertically cast and were welded in a jig to hold the bars concentric. The results are given in Table 29. The annealed specimens were brought to a yellow heat with two torches, held several minutes, and cooled in sand, or held ¾ hour at 950–1000° C. in an electric furnace. Bartels concludes that the decrease in fatigue strength by welding is proportionately less than the decrease in tensile strength, for specimens without skin, and that annealing is not beneficial to fatigue properties. A few tests on specimens containing two welds in the stressed length gave higher fatigue strengths than single welds.

Table 29—Rotating-Bend Fatigue Limits of Welded Cast Iron. Bartels, 1930

Cast Iron	Tensile Strength Unwelded	Welded	Endurance Limit (10×10^6 Cycles) Unwelded	Welded	Welded, Annealed
Without skin	27,000	10,000	10,700	8,500	8,500
With skin	38,400	35,200	28,500	25,600	22,800

The cantilever fatigue limit of gas welds, 45° V, in 1-inch cast iron (3.46 total C, 0.74 combined C, 1.33 Si, 0.106 Si, 0.66 Mn, 0.282 P) using cast-iron welding rods was 12,000 psi; the unwelded cast iron gave 13,500 psi, according to Mochel.[281]

Repeated Impact.—Bartels,[280] using the Krupp repeated impact machine and specimens similar to those described in the section on Fatigue, obtained the results shown in Table 30. The specimens were supported on a

span of 4 inches; they were 0.59 in. diameter with a circular notch 0.04 in. deep, 0.20 in. wide. The weight fell through a distance of 0.40 in.

Table 30—Repeated Impact Tests (Bartels)

Repeated Impact Value 4.4 Lb. Weight. Number of Blows in Thousands

Material	Unwelded	Oxy-acetylene Welded	Welded, Annealed
Cast iron without skin	58	2305	318
Cast iron with skin	92	3003*	73

* Subsequently withstood 53,000 blows with 3 kg. weight.

Repeated Impact Value 6.5 Lb. Weight. Number of Blows in Thousands

Material	Unwelded	Oxy-acetylene Welded	Welded, Annealed
Cast iron without skin	11	656	31
Cast iron with skin	24	954	11

Repeated Impact Value 8.8 Lb. Weight. Number of Blows in Thousands

Material	Unwelded	Oxy-acetylene Welded	Welded, Annealed
Cast iron without skin	..	22	19
Cast iron with skin	..	31	5

After each blow the specimens were rotated $^1/_{25}$ of a turn. Annealing was performed in an electric furnace, 30 minutes at 950° C. If, instead, the specimens were annealed at a red heat 5 to 10 minutes with a welding torch and cooled in sand, the repeated impact value (4.4 lb. weight, cast iron without skin) was 626,000 blows. Annealing coarsened the originally fine grain structure of the weld.

Welding Cast Iron to Other Metals

To weld cast iron to mild steel Viall[282] and Keel[100,283] advise the use of ordinary high-carbon, high-silicon filler rods, using an oxyacetylene torch and flux. The steel should be heated to the melting point first. Lyall[39] recommends arc welding (rod not stated) using moderate amperage and peening after each short run. The welding should be done in a vertical position, the slag and dirt being easily washed away. According to Connor and Featonby,[246] mild steel can be bronze-welded to cast iron using an oxyacetylene torch. The mild steel should be thicker than $^1/_8$ inch, otherwise intergranular penetration by bronze causes brittleness.

Bronze should also be used for welding malleable cast iron to steel, Miller[285] states. Brown[284] has found that heavy steel sections can be attached to cast iron by welding the steel to the projecting ends of large steel studs threaded into the casting, which is not appreciably heated by the operation. Sheet steel may be attached to cast iron by instantaneous welding in which skin fusion of the cast iron does not result in extensive distortion or structural changes.

Woofter[286] states that cast iron can be flash welded without difficulty to practically any kind of steel, stainless or cast. Cast-iron heads of internal combustion engine valves[287] have been welded to steel stems by the resistance butt process. Williams[288] has made a good micrographic study of a burn-on of chilled cast iron to mild steel brake shoes. The welding of cast iron to monel metal using a cast-iron filler rod is described by Durglund.[289] Palmer[290] mentions that a low-heat coated electrode (no details) is excellent for welding manganese bronze liners to cast-iron journal boxes without preheat.

The physical properties of welds between cast iron and mild steel have been determined by Roux,[291] Jansson[222] and Treuheit.[292] Roux made transverse beam tests on arc welds, one plate of the weld being mild steel, the other cast iron, and both 0.4 inch thick. The weld was 4 inches long, the span 2 inches. A round tool was used in three-point loading and the results are shown in Table 31.

Table 31—Cross Bending Properties of Arc Welds between Mild Steel and Cast Iron. Roux[291]

Type of Electrode	Electrode Diam. Inch	Amperage	Rupture Load Lb.	Location of Fracture
Extra mild steel	0.20	150	2550	Half in cast iron
" " "	0.20	190	3500	" " steel
" " "	0.16	115	1340	Middle of weld
" " "	0.12	90	3050	Cast iron
Cast iron	0.20	150	2250	Middle of weld
2% Nickel steel	0.20	150	3400	" " "
6 % " "	0.20	150	3100	At junction with steel plate
Unwelded cast iron			3200	Middle of weld

Jansson found that the tensile strength of butt welds of cast iron to steel using bronze and an unnamed flux was 18,720 psi. The tensile properties of mild steel bars ($^{11}/_{16}$ inch diameter) oxyacetylene welded with gray cast-iron filler rod (composition not given) were determined by Treuheit. The tensile strength was 17,000 to 22,000 psi (0.5-0.64% reduction of area, no elongation) which was equivalent to 31 to 40% unwelded. Mild steel arc welded with gray cast iron electrodes had a tensile strength of 8100 to 11,800 to 11,800 psi (no elongation or reduction of area), which was equivalent to 15 to 22% unwelded. All specimens were annealed 4 hours at 900° C. after welding.

Flame Cutting Cast Iron

Oxyacetylene

Burning vs. Melting.—The present methods of cutting gray cast iron are based on the fact that cast iron cannot be burned in the sense that steel may be burned at a temperature below the melting point. In 1914, Wüst found that the ignition temperature of pure iron was 1050° C. As carbon was added to the iron the ignition temperature increased until at 0.8% C the ignition temperature coincided with the solidus (1250° C.). At 2.25% C the ignition temperature coincided with the liquidus (1370° C.), and with further increase in carbon, cast iron can be burned only by overheating to 1400° C. Zorn,[293] who directed a stream of oxygen on the surface of cast iron contained in a crucible, found that cast iron must be raised considerably above its melting point before it may be burned. Greger[294] states that iron-carbon alloys containing up to 2.25% C can be burnt. According to Ussher,[295] 90% of the heat required for cutting is supplied by the preheating flame.

Ussher also states that cast-iron cutting consists of melting the cast iron and blowing it clear of the kerf by force of the oxygen jet. Kalisch[296] points out that the molten cast iron is at once blown beyond the field of action of the flame, and therefore cannot attain the high

temperature requisite for burning. The low heat conductivity of cast iron cannot be taken advantage of because the high heat of oxidation is dissipated in melting adjacent metal. The slag formed, which has a higher melting point than cast iron, is also a disadvantage.

Several investigators attribute all the difficulty in cutting cast iron to flake graphite. Napolitan and Plumley,[297] for instance, found that white cast iron can be cut with comparative ease, and suggested that cast-iron burning actually occurs in the opening of blast furnace tap holes. The burning of the steel lance pipe does not supply all the heat, although Zorn states that the burning of tap holes can be speeded up by using a thicker lance pipe, and a writer in *Foundry*[298] believes that at least part of the heat is contributed by the iron or copper lance pipes. Notwithstanding, Plumley found that black-heart malleable cast iron from which the outer skin had been machined was as readily cut, if preheated to a dark red, as preheated high-carbon steel or as non-preheated low-carbon steel. Proeutectoid cementite or nodular graphite retards cutting to some extent but flake graphite, as Kinsey[299] believed, being infusible, makes the whole mass of ferrite through which it is distributed infusible and difficult to burn. An explanation[300] of cutting that has never received convincing support is that at the temperature of cutting most of the graphite is in solution to form mottled iron or semi-steel, as Erbright[301] suggested.

The conclusion is that all methods of cutting cast iron depend on overheating the molten cast iron, but to what extent, if any, the cut is due to burning, is not known.

Methods.—There are two general methods of cutting: (1) Purely flame cutting, (2) flame cutting supplemented by external sources of heat. The second method is discussed in a separate section.

Type of Flame.—Excess acetylene is always used.[25, 302,303] The temperature of the excess-acetylene flame is lower than that of a neutral flame, but, as Zorn[243] found, the temperature of the former is more uniform because the excess carbon burns in the outer zones of the flame. According to Walker,[304] Zorn and Holler,[305] the luminous part of the flame should be as long as the material is thick. Ussher[295] states that the acetylene cone should be 2 to 4 inches long for castings up to 20 inches thick.

Torch.—The torch, Ussher[295] states, must have large gas passages and a larger injector than for mild steel. The oxygen pressure should be not less than 70 psi and the regulator should be heated to avoid freezing. Walker[304] found that a casting 12 inches thick required an oxygen pressure of 75 psi. Zorn[293] states that low-pressure acetylene generally cannot be used on account of the high gas consumption of the torch. The nozzle should provide more acetylene before and behind the oxygen stream than to right and left. The use of preheated oxygen is always desirable and was described by Roulleau[306] in the Annual Report of the Union de la Soudure Autogene in 1914. Cutting steadily improves as the temperature of the oxygen is increased. Patented cutting torches in which oxygen is preheated in chambers near the nozzle are described by Reinacher,[300] Fritz[307] and an Austrian welder.[308] Ordinary cutting torches are said to be unsuitable for cutting cast iron because they produce a heavy oxide skin on the surface of the casting which prevents further access of oxygen. It should be noted that far from attempting to oxidize the carbon in the cast iron by using an oxidizing flame, the torch is designed to produce reducing conditions. Perhaps preheated compressed air could be substituted for the preheated oxygen stream.

Starting the Cut.—With cutting oxygen turned off, and the heating flame adjusted with a slight excess of acetylene to prevent back-fire, the torch is held at an angle of 45° to the edge of the piece to be cut, and is played up and down the line of cutting until the metal is red hot. The upper corner is then quickly brought to a molten state and the oxygen turned on, the oxygen stream being directed 10 to 15° backward at first, according to Holler.[305] But Zorn[293] prefers starting the cut at the bottom of the edge, opening the oxygen valve slowly, and working upward until the surface is reached. The angle of the torch meanwhile is gradually steepened until at the surface the torch is nearly vertical. Reinacher[300] states that the casting should not be preheated whereas Plumley and Napolitan[297] consider preheating favorably.

Technique.—Once the cut is started the torch is given an oscillating motion, which is described in detail by Walker.[304] For castings up to 1 inch thick the torch is swung slowly sideways, then moved forward a little, swung to the other side, moved forward again, and so on. For castings over 1 inch thick the forward step is omitted. According to Ussher,[295] and Rogers and Ogden,[8] the cut is about $^1/_2$ to $^3/_4$ inch wide, and the swinging movement prevents blocking up of the bottom of the cut. Other estimates of the width of the cut are 0.2 to 0.4 in. Zorn considers the oscillating movement helpful but not essential, if preheated oxygen is used.

The tip of the innermost cone, according to Zorn,[293] should be $^1/_8$ to $^1/_4$ inch above the surface of the plate. If the burner is moved forward too rapidly and the bottom is not cut through, the torch is brought back to the origin of bridge and another cut commenced alongside the original cut. A pocket is thus left in the completed cut. To finish a cut the torch is inclined 45% to the edge and slowly moved downward. Witte[112] recommends the use of borax mixed with ferrosilicon, ferromanganese or aluminum to obtain a fluid slag.

The Torch-Steel Method.—The torch-steel method, which has had little application in this country, is used in four modifications. First, a thin mild steel plate may be placed on the surface of the cast iron in the line of the cut. Preheating is often used. The cut is made using the customary steel cutting torch. Instead of a plate, a bead of mild steel weld metal may be deposited along the line of the cut. The principle of the method is variously propounded. The steel melts into the casting and forms a low-carbon alloy which is readily burnt. The steel may otherwise be considered to burn, the heat of oxidation being sufficient to melt the cast iron underneath. In either case, the two methods are restricted to relatively thin castings.

Two other methods are also used for thick castings. In one, a mild steel wire is held near the tip of the cut. In the other, the wire is replaced by a rectangular bar slightly thinner than the width of the cut: about one-fourth of the thickness of the material to be cut, Kalisch[296] advises, but as deep as the material to be cut in the other dimension. The method is described in detail by Melhardt,[309] Granjon and Rosemberg,[51] Holler,[305] Kalisch[296] and Witt,[303] and was in use as early as 1914. Greger[294] considers the use of a mild steel rod essential for good cutting. Walker[304] mentions the injection of iron powder into the oxygen cutting jet to increase cutting speed, and Zorn[293] mentions the use of aluminum powder for a similar purpose. The method is dangerous, however, for plugging of the nozzle then causes back-firing.

A review of methods of flame cutting cast iron has been published in *Oxy-Acetylene Tips*[310] since the completion of this review. The oscillating torch and ma-

noeuvres at starting and finishing the cut discussed above are advocated for white, gray and malleable cast iron, as well as semi-steel and duriron. The wedge method employing mild steel welding rod is used for low-grade cast iron. Large sections should be cut warm, if possible. Aluminum or ferrosilicon may be used as flux to form a fluid slag.

Cutting Velocity.—An Austrian welding magazine[308] states that the horizontal distance between top and bottom of the cut should be 30 to 40% of the thickness cut, the torch being inclined 20 to 30° to the vertical. Table 32 contain Zorn[293] results.

Table 32—Flame Cutting Velocity for Cast Iron (Zorn)

Thickness—Inches	Cutting Velocity, Feet per Hour
1	10
2	8.5
6	6
12	4.5

Witte[112] reported 7 ft.-hr. for castings $1^3/_4$ inches thick, and Milne[311] found 12 to 13 ft.-hr. for castings 1 in. thick, 11 ft.-hr. for castings $5^1/_2$ in. but Rohrbacher[312] in 1922 could cut only 4 feet per hour in 6 in. thick castings. The figures do not differ much from those given by Zorn who, indeed, stated that cutting speed is different for different cast irons and different operators. Much lower speeds are reported by a French welder:[331] 7.9 and 4.3 ft. per hour for castings 1 and $2^3/_4$ in. thick, respectively. A speed of only 5 ft.-hr. in castings 0.6 in. is reported for the torch-steel method.[339] *Industrial Gases*[313] has measured the gas consumption in cutting cast iron press parts 2 feet thick.

Special Cast Irons.—White cast iron, according to Plumley[297] and Zorn,[293] is easier to cut than gray, but Milne[311] states that soft gray cast iron requires slightly less time to cut than white.

An anonymous writer[314] starts the cut by allowing drops of overheated metal from a wrought-iron rod to drop on the white cast iron, which is at once melted through. White-heart and black-heart malleable cast irons (1.8% C) are easy to cut, according to Wiss,[315] and Plumley[297] found that black-heart was easily cut if it were preheated to a dark red. Zorn[293] states that high manganese facilitates cutting but that high silicon is a disadvantage.

The method of severing large cast iron parts by heating the surface with an oxyacetylene torch followed by cracking is described in *Industrial Gases.*[316]

Arc Cutting.—Cutting cast iron with water-soaked metal electrodes at 75 volts and high amperage is described briefly by Sonderegger[317] and Rogers.[333] Arc cutting cast-iron pipe under water is briefly described by Hagglund.[340] Schimpke and Horn[104] in the 1935 edition of their text-book mention that steel electrodes are still used for cutting cast iron. Cutting cast iron with the carbon arc is described by Candy,[318] who gives Table 33 applicable to cutting with 600 amps. For thin castings (2 inches thick) the carbon arc appears to be faster than oxyacetylene cutting; for thicker castings the carbon arc is slower. A writer in *Machinery*[319] states that to cut cast iron 1 inch thick with the carbon arc, the arc length should be 1 to 2 inches at 700 amps., the process being almost entirely melting.

Table 33—Carbon Arc Cutting Velocity Candy[318]

Size of Section	Time of Cutting Minutes
6 inches diam.	10
12 " "	45
16 " "	85
12 " "	60
16 " square	110

Depth of Cut Inches	Linear Ft.-Hr.
2	12.5
4	3.5
6	2.0

Heat-Affected Zone.—Wiss[315] found that whereas there is only occasionally a ledeburitic surface on the cut surface in malleable cast iron, gray cast iron cuts are usually coated with a hard layer that is unmachinable except with tungsten carbide tools. Reinacher[300] found a hard zone about 0.04 inch thick back of the cut edge and Zorn a white zone 0.02 inch thick, which caused surface cracks. If the cut is subsequently welded, the hard zone disappears, according to Zorn.[293] Kalisch[296] observed that a heat-affected, sorbitic zone extended 0.2 inch back of the cut edge, but there was no hard layer. Micrographs showed that graphite flakes extended to the edge of the cut. The cast iron contained 3.55 C, 2.45 graphite, 2.11 Si, 0.65 P, 0.05 S; the mild steel auxiliary rod contained 0.11 C, 0.47 Mn, 0.11 P; the slag analyzed 0.71 C, 85.3 Fe.

Shorter,[320] Kessner,[321] Zorn[322] and Bossert[323] have discussed the flame hardening of gray cast iron. A French investigator[332] obtained the results shown in Table 34.

Table 34—Brinell Hardness of Flame Hardened Cast Irons

Combined carbon	0.49	0.96	2.09	0.74	0.64
Graphite	2.61	1.92	0.56	2.00	2.30
Silicon	1.91	1.42	1.01	2.26	1.58
Nickel	..	..	..	0.48	0.7
Chromium	0.06	0.11	0.24	0.36	0.08
Original hardness	235	350	367	269	292
Hardness after quenching	450	560	578	450	450

Suggested Research Problems and Studies in Welding Cast Iron

1. A comprehensive study of the mechanical properties of welds in several representative alloy cast irons of average thickness made by all processes (gas, arc, bronze, resistance, etc.). The tests should include tensile, cross bending and hardness properties, and microstructure and should be performed on specimens cut from weld metal and heat affected ones, with and without optimum heat treatment. Unalloyed cast iron welded with austenitic steel filler rod should also be studied.
2. Which of the explanations of hard spots and white zones in welded cast iron are not important?
3. A study of the physical properties of monel welds in cast iron.
4. In what way does oxidation of the surface of the cast iron affect its "tinning" and "brazability" in bronze-welding?
5. A study of porosity in fusion welds in cast-iron and high-carbon steels from the standpoint of the "rimming" reactions.
6. A study of the effects of ordinary variations in the composition of cast iron (carbon, silicon, manganese, phosphorus) on ease of welding and quality of the weld.
7. A study of the torsion test as an indication of the toughness of fusion and bronze welds in cast iron.
8. A study of shrinkage distortions and stresses in fusion and bronze welds in cast iron.
9. A study of globule transition in arc welding cast

iron, including the spectroscopic constitution of the arc between cast-iron electrodes.

10. What is the composition of the slag from cast irons cut by all processes?

11. Under what conditions of temperature and flame adjustment does bronze-welding metal penetrate the phosphide eutectic of gray or white cast iron? What effect has the composition of cast iron or bronze-welding rod on this penetration? What effect has the penetration on the mechanical properties of the joint?

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Appendix A

Average Properties of Unalloyed Cast Iron and Cast Steel

Chemical Composition		Gray Cast Iron	Cast Steel	Black-Heart Malleable Cast Iron
Carbon		2.0–4.0	0.1–0.5	1.00–2.00
Silicon		0.25–2.5	0.2–0.5	0.60–1.10
Manganese		0.4–1.0	0.3–1.0	under 0.30
Phosphorus		0.1–1.1	0.01–0.09	under 0.20
Sulphur		0.05–0.16	0.02–0.08	0.06–0.15
Physical Constants				
Specific Heat	20–100° C.	0.131	0.11	0.122
(Calories per gram per	20–700	0.154		0.159
degree Cent.)	20–1300	0.218		
Heat of Fusion (Calories per gram)		47	65	
Thermal Conductivity				
Gram-calories per	50° C.			0.145
Second per sq.	100° C.	0.094–0.135	0.10	0.137
Centimeter per	200° C.	0.092–0.128		0.115
Degree centigrade	400° C.	0.087–0.115		
Per centimeter		(High Mn or Si up to 2.5% gives the lower values)		
Coefficient of Thermal				
Expansion	0–200° C.		0.000013	0.000011
(Expansion in length per	0–400		0.000014	0.000013
unit length per degree	0–700			0.000015
Centigrade	0–1070	0.00009–0.000017		
Shrinkage Allowance	3.7% C	0.07–1.65		
(Contraction from Pat-	3.1% C	negative to 2.0		0.98
tern Size, %)	2.3% C	5.0–6.0 (white)		1.63
		(1/8 inch per ft.)	(1/8–1/4 in. per ft.)	(1/4 inch per ft.)
		From freezing temperature to 1070° C. there is an expansion of 0.05 to 0.25% due to graphitization and gas evolution. Shrinkage decreases with increase in mass but increases with increase in tensile strength. The difference in specific volume of gray iron and white iron in the molten state at eutectic temperature is only 1%, according to Sauerwald and Widawski (1926).		
Specific Gravity		7.0–7.3	7.86	7.15–7.45
		7.69 (white)		
A_1 Point ° C.		720–730	720	
Melting and Freezing	3.7% C.	1150	Low C (0.1–0.2 C) 1435–1500	No reliable data
Range (degrees Centigrade)	3.3	1150–1155	Regular C (0.2–0.35 C) 1420–1480	
	3.0	1150–1250	High C (0.35–0.5 C) 1125–1465	
	2.2	1150–1350	(1125 applies to a casting containing 1.7% C)	
		Depending on rate of heating and size of graphite, the melting point lies somewhat above the freezing point. With extremely rapid heating, preventing solution of graphite, the melting point would be that of steel of the same combined carbon content as the cast iron		
Fluidity (Viscosity, Centipoises)		4.0% C, 1250° C. 2.1	1% C, 1400° C. 2.6	
(Thielmann, 1933)		4.0% C, 1400 1.5		
		2.5% C, 1400 2.3		
Surface Tension (dynes per square centimeter)		1267° C. 936		
		1318° C. 914		
		1150° C. 920 (Sauerwald)		
		A cast iron with 7.44% P had 100 dyn/cm.^{2} higher surface tension than one with 0.49% P.		

Mechanical Properties	Gray Cast Iron	Cast Steel	Black-Heart Malleable Cast Iron
Short-Time Tensile Strength, (psi)	20° C. 20,000–60,000 430° C. no decrease 650° C. 50% decrease	40,000–140,000 0 to 20% less 50 to 60% less	54,000 54,000 11,000–14,000
Yield Strength in Tension (psi)	none	15,000–120,000	36,000
Elongation, % In 2 in.	0.2	low C 13–36 regular C 6–40 high C 1–30	18
Modulus of Elasticity in Tension (psi)	10,400,000 to 22,600,000 (non-linear stress-strain curve)	28,500,000 to 30,000,000	25,000,000
Modulus of Elasticity in Shear (psi)		12,000,000	12,500,000
Impact Value (ft.-lb.)	Reliable results are not obtained using notched specimens.	15–50 Izod	7.75 Charpy
Range in Which Growth May Occur (degrees centigrade)	400–800 (some cast irons are immune)	immune	immune up to 760° C.
Brinell Hardness	130–210 gray 321–534 white up to 600 heat-treated		
Electric Resistivity (millionths of an ohm per centimeter cube)	3.7% 78 20° C. 2.4% C 218 20° C.	15–25 20° C (increases with increasing carbon content.)	20° C. 28–37 400° C. 50–67 700° C. 82–109
Electric Conductivity in the Liquid State Bornemann & Wagenmann 1914	% C 1450 1550 1650° C. 0 .. 0.74 0.72 1 0.67 0.66 0.64 3½ 0.65 0.64 0.63		

Greaves (British Steel Castings Research Committee Report, 1936) states that the equivalent values of the contraction of pure iron referred to the volume at atmospheric temperature and to the volume of solid at the melting point are:

% Volume of solid at:	20° C.	1530° C.	Remarks
Liquid Contraction per 100° C.	0.8%	0.75%	*A*
Freezing " " "	3.0	2.85	*B*
Solid " " "	7.35	6.96	..

A—increased by C, Si, Mn, P; decreased by Cr and Al.
B—with 1%.
C—4 to 5% but additional carbon decreases the value.

The value 7.35 is obtained by multiplying the linear contraction by 3. The linear contraction, 2.45%, is reduced to about 2.3% at 0.8% C. The total volume contraction from 1600 to 20° C. (expressed as per cent of volume at 20° C.) is increased from 10.5 to 14.0% as carbon is increased from 0.06 to 1.0%. Small quantities of alloying elements have no effect, except Al.

Appendix B

Average Properties of Monel Metal and 60–40 Brazing Rod

Chemical Composition	Monel	Bronze-Welding Rod
Ni	68	
Cu	29	60
Fe	1.5	
Mn	1.1	
C	0.15	
Si	0.1	
Zn		40 (1% Sn, Si, Mn or other element)
Thermal Conductivity	0–100° C. 0.06 c.g.s. units	Approx. 0.25 cal./cm.° C. sec.
Coefficient of Thermal Expansion, c.g.s.	25–100° C. 0.000014 25–300° C. 0.000015 25–600° C. 0.000016	23–308° C. 0.000018
Melting Range, ° C.	1300–1350	895–905
Specific Heat	20–400° C. 0.127	20–100° C. 0.384 60% Cu
Electric Resistivity microhms per Cm. Cube	42.5 (256 ohm/mil foot)	
Specific Gravity	8.80	8.33
Recrystallization Temp. ° C.	430–650	300
Tensile Strength, psi (cast)	60–80,000	46,500 to 48,000
Elongation in 2 In., % (cast)	40–20	8 to 25
Reduction of Area, %	40–20	15–40
Yield Strength (0.1%), psi (cast)	30–40,000	13,500 to 17,000
Proportional Elastic Limit, psi	18–25,000	
Modulus of Elasticity in Tension, psi	26,000,000	16,000,000
Shear Modulus, psi	9,000,000	

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The Welding of Copper and Its Alloys—A Review of the Literature to Jan. 1, 1936

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Foreword

As a part of the work of the Welding Research Committee (Fundamental Research and Literature Subcommittees) sponsored by the American Society of Electrical Engineers and the AMERICAN WELDING SOCIETY *under the auspices of the Engineering Foundation, the following critical review has been made. Parts 1 to 4 were published in February 1937 issue,* AMERICAN WELDING SOCIETY JOURNAL

PART 5

Copper-Silicon Alloys

Silicon is used both as a deoxidizer for copper and copper alloys and as a major alloying constituent. Its use as a deoxidizer has been reviewed under Parts 1 and 2. In Part 5, we shall review the literature on the copper-silicon alloys where the latter element is used as a strengthening and hardening agent usually with a minor third element as manganese, zinc or tin.

One of the earliest of the copper-silicon alloys is that developed by Charles B. Jacobs and, with the addition of manganese, patented by him in 1925 under the name Everdur.[124] The Everdur was the result of Jacobs' attempt to find a nonsparking, high-strength copper alloy to replace the gun metal bronze. The Jacobs' alloy with silicon up to $4^1/_2\%$ and approximately $^1/_3$ as much manganese proved to have better hot and cold ductility than the gun-metal bronze as well as higher strength and improved weldability. Since 1925, other copper-silicon alloys have been commercialized—usually with additions of the third element as zinc, tin or nickel.

The literature on the welding of the copper-silicon alloys is comparatively scanty on account of the newness of such alloys.

Weldability

The weldability of the copper-silicon alloys in all types of welding has been a large factor in their popularity. This property is ascribed to several features (*a*) the low thermal conductivity, (*b*) the good hot ductility (except in a narrow temperature range just below its melting point), (*c*) the non-sensitivity of the metal to overheating and (*d*) the presence of a flux-like, continuous, liquid glass film which covers and protects the molten weld metal.

Oxyacetylene Welding

The preparation of the copper-silicon alloys for welding is in general no different from that of copper. Scale and grease should be removed from the region of the weld. For thickness of $^1/_8$ in. and less square edges may be used while for greater thicknesses beveled edges are to be preferred though, if given sufficient space between them, square edges may be used on sheets up to $^3/_{16}$ in. in thickness.

Swift[125] suggests a 45° bevel (90° single vee groove) on the edges of $^1/_4$-in. and $^3/_8$-in. thick plate and a double bevel of 30° on $^1/_2$-in. and thicker plate. The latter will, of course, make a double vee welding groove of 60°. He further recommends that plates up to $^3/_8$ in. in thickness be welded in one pass while thicker plates should be welded in two or more passes. As in the case of copper, when welding the second pass or when welding sheets rigidly attached, the starting point should be several inches in from the end.

In an article on the Welding of Everdur Pressure Vessels,[126] Powell and the reviewer found that the simplest way to avoid hot short checks and fractures in the weld was to make the weld fast. It was stated: "Everdur should be welded as rapidly as possible. The reason for this is that it is desirable to keep the shrinkage stresses in the red hot metal as low as possible, and, of course, the best way to avoid them is to cut the heat flow into the base metal as short as is consistent with good union. This rule holds whether the welding be done with the oxyacetylene torch or the electric arc. Moreover, the most homogeneous solution the metal can have is that of its liquid state. Hence, the quicker the metal is solidified, the more uniform will be the cold metal and the finer its structure."

Horn and Tewes[128] came to the same conclusion saying, "The faster it (Everdur) is welded, the finer grained it is and the better it agrees to the base metal."

Flame Adjustment.—The copper-silicon alloys weld best with a concentrated flame and a small weld pool. This result can be obtained best with a slightly oxidizing flame.[127,125] The reviewer[129] in an investigation made in 1929 shows that an oxidizing flame made the fastest and strongest welds, saying "The reason for this is that we get a much greater concentration of heat than with the neutral flame while the excess oxygen does no harm (to Everdur) as it would to bronze or steel. The molten metal is protected by a thin coating of silica glass which spreads evenly over the metal in an impervious layer."

Horn and Tewes[128] in an extended investigation on the oxyacetylene welding of Everdur arrived at the same conclusion. They said, "An oxyacetylene flame with about 10% excess of oxygen yielded welds of extraordinarily better results than those made with the neutral flame." They remark further that, "the excess of oxygen is much smaller than that which is usual (up to 30%) in the welding of brass."

As for the size of the torch, Horn and Tewes[128] found that "flames such as those ordinarily used in copper welding are much too strong for Everdur." Swift[125] prefers an oversize torch tip with the gases throttled to give a "soft" flame.

Welding Rod.—There is a general agreement that a welding rod of a composition similar to that of the base metal furnishes a satisfactory filler metal for the copper-silicon alloys.[125,126,127,128] Thus the weld is a true fusion weld. The reviewer pointed out that the copper-silicon alloys could also be bronze-welded with welding rods such as Tobin bronze,[127] remarking ". . . . .the one thing of prime importance is that the seam to be welded be painted liberally with a good flux before the heat is applied."

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Swift[125] shows appropriate welding rod sizes as $^3/_{16}$ in. diameter for plate from $^1/_8$ in. to $^1/_4$ in. thick, $^1/_4$ in. diameter rod for $^3/_8$-in. thick plate welded in one pass and $^1/_4$-in. or larger rod for thicker plates depending upon whether the weld is made in multiple passes or one pass. Horn and Tewes[128] say, "It is advisable not to use too small a welding rod, since there would be danger of overheating the metal."

Set-Up for Welding.—The reviewer remarks[127] that "the oxyacetylene method works best when the plates are free to come and go. An attempt to oxyacetylene weld with the edges of the seam clamped is prone to result in hot short cracks. If the end of the seam is open $2^1/_4$ to $2^1/_2$% of the length of the seam, it will usually close properly as the seam is finished." Swift[125] prefers to leave the end of the seam open 2%.

Swift[125] recommended a light back-up bar tack-welded to one edge of the sheet at the seam. In many cases, however, the reviewer finds that no back-up is necessary. Where the welder has the requisite skill, he can control the metal by suitable manipulation of the torch to secure proper fusion without loss of metal.

Flux.—Swift[125] recommends a finely ground brazing flux on the edges of the plate and on the rod. Rods are ordinarily coated with the flux by heating them moderately and sprinkling the dry flux on them. Horn and Tewes[128] used a commercial flux paste sold in Germany for copper welding. This is thought to be a mixture of fused borax, boric acid and sodium chloride. In this country, a 10% addition of sodium fluoride is used instead of the sodium chloride.

The reviewer's[127] statement of 1933 is in general correct, thus "Plenty of flux, in which the essential constituents are fused borax and boric acid, is required (for gas welding). A good commercial brazing flux will usually answer. This is one respect in which the gas welding of Everdur differs from the arc welding. The latter calls for little or no flux."

After-Treatment of Welds.—Horn and Tewes[128] made a careful investigation of several after-treatments in oxyacetylene welds in Everdur. They found by actual trial that a simple anneal and quench had little effect on the strength of the weld, if anything causing a slight lowering.

Their best results were obtained with welds which were *hot hammered.* This treatment improved both the tensile strength and ductility. No further improvement was obtained by annealing the welds which were hot hammered.

In American practice the hot forging of welds is seldom resorted to. However, there is another way in which the same result may be obtained, i.e., cold peening and complete or local annealing. This method was explained in connection with metallic arc welding of Everdur by Powell and Hook.[126] Such treatment would show refinement in the grain of a gas weld but would in all probability show no improvement in ductility.

Metallic Arc Welding

The copper-silicon alloys are, unlike the brasses, weldable by the metallic arc. If the zinc be present in small proportions, say 1 or 2%, satisfactory welds may be made in spite of its loss by vaporization in the arc.

There is considerably more in the literature on the carbon arc welding of the copper silicon alloys than there is on the metallic arc welding. This is due to several causes as (*a*) with the bare rod, it can only be done down hand, (*b*) up to the end of 1935 no satisfactory coating had been developed, (*c*) in the "as-welded" condition the weld metal is rather low in ductility and (*d*) the technique is so markedly different from that of steel as to require a special skill.

Under the heading "hardened copper," the reviewer[129] discusses the welding properties of the copper-silicon alloys as follows: "To the welder familiar with the flowing properties of low-carbon steel, this silicon-manganese copper would seem to flow marvelously free. Under the torch or arc, it flows like water and will lie quiet under its protecting sheet of silica, even in the full blaze of the electric arc. It has, however, one weakness which must be guarded against, it is hot short in the solidification temperature range just below its melting point. Thus, as the metal cools down from the molten to the solid state, precautions must be taken to avoid tensile stresses. Also since selective freezing is a function of time, we should hasten the cooling through this range, the maximum extent. The high concentration of heat over as small an area as possible is at once apparent."

Preparation.—Little is said about preparation of the sheet metal but as was pointed out above metallic arc-welding of the copper-silicon alloys can be done only down hand. Hence, in making a set-up, the sheets must be arranged in a flat or nearly flat position. Moreover, since the metal is so very fluid under the arc, it must be very carefully backed up. Also since the seam is run so rapidly, the sheets are usually tack-welded and the weld run as between rigidly held plates. Considerable further detail is given by Powell and Hook.[126]

Welding Rod Procedure.—In all cases cited,[125–129] the filler rod was identical in composition to the base metal. The rods were used bare and as *positive* electrodes.

Powell and Hook[126] show much higher current values with bare Everdur electrodes than is commonly used on bare steel of the same size. They found, for instance, that 190 amperes was about correct for a $^5/_{32}$-in. diameter bare, Everdur electrode. They also found that any thickness of plate may be welded by a suitable number of passes.

After-Treatment.—The low ductility of the metallic arc welds, led Powell and Hook[126] to try various after-treatments. A simple anneal of the finished weld was found to occasion no material improvement in either strength or ductility. The severe peening of each deposit by the use of an air hammer was found to improve strength and ductility of the weld and to cause refinement of the grain on all except the outside deposits.

Further investigation[126] showed that if each $^3/_{16}$-in. depth of deposit were peened with an air hammer, the outside layers also being peened thoroughly and the entire weld annealed locally at a dull red heat estimated at 650° C. (1202° F.) by the slow passage of an oxyacetylene torch along the seam, there would be a decided improvement in both ductility and strength of the weld and the grain would be refined as well. This treatment was used on the welds on two large Everdur tanks and found to yield good results.

Carbon Arc Welding

Owing to the better control of the weld metal when welding relatively thin sheets and the better strength and ductility obtainable with the carbon arc in the "as-welded" condition as compared to the results obtainable with the metallic arc with no after-treatment, the carbon arc has largely superceded the metallic arc in the welding of the copper-silicon alloys.

Swift[125] reports that "the preparation for (carbon arc) welding is the same as in the use of the oxyacetylene or metallic arc method." He further recommends the use of copper backer bars grooved under the seam in order

to permit complete fusion at the root of the weld. The reviewer has found that such copper backer bars are excellent for carbon arc or metallic arc welding but that a backer having a lower heat absorbing capacity such as asbestos or thin sheet steel is preferable for oxyacetylene welding of Everdur.

As for filler metal, all of the published information, as in references,[125–130] points to the use of a welding rod similar in composition to the base metal. In articles by Swift[125] and the reviewer[127] the use of the up-turned sheet edges, flat welding wire pinched between the abutting edges and round welding rods laid in place on the edge are illustrated. In such disposition, the carbon arc is merely carried along the seam to melt the metal.

Swift[125] lays down an excellent rule as to the character of the arc saying, "In all welding of Everdur (copper-silicon alloys in general) the pool of molten metal should be as small as possible. A *short* arc is therefore necessary —$^3/_{16}$ in. is a maximum—with 20 volts across the arc." This is another way of saying as has been pointed out before in this review that the welding should be done as quickly as possible.

Flux is not nearly as important in carbon arc welding as in gas welding. Swift[125] speaks of "dusting" the flux on the work or "painting it on with dry alcohol." The flux composed of 90% fused borax plus 10% sodium fluoride mentioned in the articles of 1929[129] and 1931[126] is frequently used for carbon arc welding.

The literature appears to be silent on *after-treatment* of carbon arc welds though there is no reason why that described for metallic arc welding[126] cannot be used to advantage on carbon arc welds.

Resistance Welding

There seems to be no doubt but what copper alloys in which silicon is present as a major alloying constituent are among those most readily welded by the resistance process.[28] This statement applies to the brasses as well as those alloys higher in copper.

The process is used commercially on many types of articles. In the article on small Everdur tanks[127] the manufacture of small kerosene containers by this method is described. Uses of air tight resistance seam welds in metal as thin as 0.010 in. are also cited.

In the latter article[127] the technique is stated thus, "The proper technique for resistance spot and seam welding of Everdur (copper-silicon alloys) differs materially from that used in welding steel sheets. In Everdur, as in most copper alloys, the metal must be molten to make a weld. It does not have a weldable, plastic range as does steel. The pressure control, therefore, must be more accurate and it is usually much lighter."

As is true of most copper alloys[28] the current value is relatively high with very short heat-on periods.

PART 6

Copper-Nickel Alloys

Since this review is restricted to alloys carrying 50% or less of nickel, a large part of the literature dealing with monel metal and pure nickel is not reviewed herein. The nickel silver alloys have already been discussed under Part 3.

The copper-nickel alloys are of ancient origin. Wadhams[130] tells of a cupro-nickel coin of the Kingdom of Bactria carrying 20% nickel struck about 235 B.C. which is not far removed in composition from our present 75:25 cupro-nickel five cent piece. It was not, however, until Fleitmann[131] in 1879 discovered that the addition of 0.05 to 0.10% of magnesium would correct brittleness arising from sulphur, and unsoundness arising from carbon monoxide gas, obtaining thereby metal consistently ductile that the cupro-nickels were commercially successful.

In general, the copper-nickel alloys differ in their welding characteristics from both copper and steel. The heat conductivity is lowered sharply by the addition of nickel which fact tends to facilitate welding operations but difficulties are interposed (*a*) by the refractory, insoluble nature of the nickel oxide and (*b*) by the fact that the cupro-nickels are prone to have a relatively long range of low ductility in the cooling down period.

The nickel oxide makes the welding of the cupro-nickels somewhat difficult. Besides being closely-clinging, refractory and only slightly soluble in the common borax-base fluxes, the nickel oxide causes the surface of the weld metal to freeze in a rough cinder-like manner.

Moreover, nickel confers on the alloy an increased tendency to dissolve such gases as hydrogen[18] which are prone to cause porosity as the metal freezes. Again, Smithells[18] shows that nickel is extremely sensitive to sulphur forming with it an eutectic melting at 644° C. (1191° F.). As little a proportion of sulphur as 0.005% will cause trouble making the metal both hot short and cold short. Merica and Waltenburg[132] have shown that either magnesium or manganese can be used to correct this difficulty in the copper-nickel alloys. Most of the cupro-nickels are protected in this manner.

Oxyacetylene Welding of Copper-Nickel Alloys

The preparation of the base metals for welding differs little in the cupro-nickels from that of the other copper alloys. For thin sheets, upturned edges may be used while for thicknesses up to $^1/_8$-in. square edges, properly spaced, may be employed. For greater thicknesses, a 90° open vee is satisfactory.

In setting up for welding, it is desirable to give the metal as much freedom as is consistent with perfect matching of the two edges of the seam in order to avoid hot short cracks and shrinkage checks. Flocke and Schoener[133] in discussing the welding of monel metal and nickel emphasize the economy and necessity of the use of skeleton jigs for holding the edges in line for welding. Such instructions apply equally well to the cupro-nickels.

The procedure of the actual welding operation is pointed toward the avoidance of (*a*) nickel or copper oxide and (*b*) the burning out of the protective deoxidizers. Flocke and Schoener[133] and McKay[134] have given an excellent exposition of the desirable flame characteristics in welding high nickel alloy which applies equally well to those lower in nickel, thus:

"For the best results . . . . . there should be little puddling; the molten pool should be kept quiet with the tip of the luminous flame just touching its surface. Puddling burns out the deoxidizers which are important ingredients of these alloys although present in very small amounts. These deoxidizers are sufficient to insure good physical properties and are present in sufficient excess in the welding rods. However, if the welder puddles and boils the metal excessively he will 'take the life out of it' by removing the deoxidizers and obtain a brittle and gassy weld. . . . It is well to keep the end of the welding rod well within the flame so as to prevent the oxidation that would result if the hot rod were brought outside of the protecting flame envelope into the open air."

Horn, in a very complete investigation into the welding properties of the 80:20 and 70:30 cupro-nickels[135] estimated the torch size which would give a soft ("weak") flame desirable for the welding of such metals as that

which would burn 100 liters of acetylene per hour for each millimeter of sheet thickness (5.6 cubic feet per hour per $^1/_{16}$ in. of thickness). He adds, "If larger burners are used, the weld will be overheated and brittle . . . . . Overheating the weld must be avoided under all circumstances."

Sometimes a thin sheet is to be welded to a thicker sheet or casting in which case Horn[135] recommends that the latter be preheated with another torch several inches ahead of the welding torch rather than to use a larger size to compensate for the heavier metal on one side.

Horn[135] further suggests that plates up to 2.5 mm. (0.10 in.) thick can best be welded with the torch flame directed ahead—forehand welding—while thicker plates are best welded with the torch directed backward—backhand welding.

Horn,[135] using a strictly neutral flame, recommends that the luminous, inner cone be held 5 mm. ($^3/_{16}$ in. nearly) from the surface of the thin plates and 10 mm. ($^3/_8$ in. nearly) from the surface of the thicker plates. The greater distance of the cone from the thicker plates is necessary as with backhand welding, there is a more direct impingement of the burning gases on the metal in the weld pool than in the forehand welding of thin sheets.

Flocke, Schoener and McKay[134] prefer a slightly reducing flame with a white feather of excess acetylene $^1/_4$ in. to $^1/_8$ in. beyond the inner cone. With this character of flame the tip of the white, acetylene feather can be brought into contact with the weld pool.

Welding Rod.—With care exercised in the handling of of the torch, satisfactory connections can be made with welding rods or strips identical in composition to the base metal.[134,135] However, some authorities prefer special welding rods having somewhat greater proportions of the protecting elements than are necessary in the cupro-nickel sheet. Thus, Shimpke and Horn[47] quote patents for welding rods calling for definite proportions of manganese to nickel and others in which vanadium is used instead of manganese. They say further that magnesium in proportions of 0.10 to 0.20% hinders the dissolving of carbon monoxide in the weld metal and improves it.

The reviewer[136] has found that the use of a small addition of silicon to the welding rod[137] aids in the formation of a continuous, fluid, protective film on the weld pool which partially suppresses the formation of nickel oxide, partially prevents the loss of the deoxidizing and desulphurizing elements and keeps the welding gases out of the weld metal. The film also helps keep the weld metal in place improving it for position welding.

All authorities are agreed that the welding rod should be kept within the protective atmosphere of the welding torch (not withdrawn into the air) and that the welding of a seam should be *as nearly continuous as possible.*

Flux.—It is generally agreed that a flux should be used on both top and bottom of the plate. The welding rod should also be coated with flux as a preliminary to the welding operation instead of withdrawing the hot end of the rod from the torch envelope and dipping same in dry flux as is commonly done in bronze- or copper-welding.

Crawford and Schoener[138] recommended a flux for nickel alloys made up of 50% boric acid and 50% fused borax mixed with alcohol but Flocke and Schoener in a later article[133] say that "Ordinary borax is not considered a suitable flux for monel metal or nickel. It improves the fluidity of the molten metal, but when present in large proportions, the extra fluidity is accompanied with lack of ductility in the finished weld. It does not dissolve the metallic oxides sufficently to produce the clean appearance sometimes necessary, and it is rather difficult to remove." They recommend instead a special flux (the exact composition not being given) which is mixed with boiling water and painted on rod and sheets.

Shimpke and Horn[47] recommend the use of manganese and phosphorus containing fluxes. Horn in his later work[135] says that paste fluxes such as those used for copper are recommended. Such fluxes in Germany are usually mixtures of boric acid, fused borax and sodium chloride mixed with distilled water. Miller[92] quotes the Revue de la Soudre Autogene, March 1929, to the effect that the problem of welding nickel has been solved by the use of a flux containing manganese . . . . it being stated that the manganese added to the flux is preferable to the addition of manganese to the welding rod composition.

The reviewer reports the successful application of silver brazing alloys and methods[136] using the oxyacetylene torch and silver brazing fluxes to the joining of 70:30 cupro-nickel tubing to cast brass, cast bronze or cupro-nickel fittings.

Arc Welding of the Cupro-Nickels

The reviewer found no references that were concerned directly with either the carbon arc welding or the metallic arc welding of the cupro-nickels.

However, the directions given for the arc welding of monel metal and nickel given in various references[133,139] et al. can be applied in a large measure to the cupro-nickels. It is the reviewer's opinion that the successful metallic arc welding of cupro-nickel awaits upon the development of a satisfactory flux-coated, cupro-nickel welding rod.

Resistance Welding

The reviewer has shown[28] that nickel added to copper —the cupro-nickels; and to brass—the nickel silvers—produces alloys of exceptionally good resistance spot and seam welding characteristics. In the case of resistance and "flash" *butt* welding, difficulty may be encountered from nickel oxide if the follow-up be too slow. Moreover, hot short cracks may develop if too much pressure is applied in the push-up. In general, a moderately high heat, sufficient to cause fusion in a relatively short period, with a fast, light pressure is desired.

PART 7

Copper-Cadmium, Copper-Aluminum and Copper-Beryllium Alloys

For several reasons the literature on these three important classes of copper alloys is sketchy and incomplete. Thus, the copper-cadmium group of alloys find their most important field of usefulness in the manufacture of electrical conductors which are more frequently soldered or silver brazed than welded. The copper-aluminum group—the aluminum bronzes— are more often used for mechanical parts where high strength and corrosion resistance are required. Only recently has there been a demand for the welding of the aluminum bronzes. Beryllium copper finds its economic field in small parts where a combination of two or more of the following properties are desired, (*a*) high strength, (*b*) excellent resilience, (*c*) good wear resistance, (*d*) good corrosion resistance, (*e*) good response to heat-treatment and (*f*) moderately good electrical conductivity. Being a new, and somewhat high-priced alloy, comparatively little of it has been welded.

The effect of small additions of cadmium, aluminum or beryllium in copper has already been discussed in Part 1. Aluminum in brass as a means of keeping down the vaporization of zinc is discussed in Part 3.[21,69,96,93] Only a few hundredths of a per cent is required for this purpose. The same may be said for beryllium. In the aluminum bronzes, however, we may have ten per cent or more of aluminum, the remainder being, for the most part copper.

Oxyacetylene Welding

No references are available on the use of *copper-cadmium* alloys as base metals. Matteoli[33] cites the use of copper-cadmium alloy as a filler rod on electrolytic copper sheet. As was pointed out in Part 1, cadmium in copper is even more readily vaporized than zinc. Matteoli[33] brings this point out by analyzing the filler metal before and after being deposited on copper with the oxyacetylene torch, thus:

Cadmium % in Welding Rod	Cadmium % in Welded Seam	Loss of Cadmium %
0.93	0.53	43
0.78	0.50	36
0.55	0.35	36

The reviewer is not certain but what part of the apparent loss of cadmium may be due to dilution of the welding rod deposit with the base metal.

In general, the reviewer would expect no great difficulty in gas welding cadmium copper. It has quite a high heat conductivity but, as was pointed out in Part 2, this can be overcome by suitable welding and preheating torches. While a considerable proportion of the cadmium would be lost in that portion of the metal which was melted, there should be sufficient left to keep the weld metal deoxidized.

Similarly, there seems to be few if any references to the oxyacetylene welding of *aluminum bronze*. It has been the reviewer's experience that aluminum bronze can be gas-welded. The operator, however, experiences considerable difficulty with the refractory, insoluble aluminum oxide which floats on the surface of the weld pool and clings tightly to the solid metal. In discussing the effect of aluminum, the reviewer[28] remarks "In oxyacetylene welding, the aluminum oxide film does interfere with the flow of the metal, particularly with eight or more per cent of aluminum. The 5% aluminum bronze can, however, be satisfactorily welded with the torch or the arc."

Beryllium copper is even more difficult to weld with the oxyacetylene torch and for a similar reason, the beryllium oxide is quite as refractory, insoluble and closely-clinging as is the aluminum oxide.

Metallic Arc Welding

No publications were found describing the metallic arc welding of either or these three groups of alloys. It is the reviewer's opinion that little will be done on the metallic arc welding of the copper-cadmium alloys. As in the case of the brasses, the minor constituent, in this case cadmium, is too easily vaporized.

On the other hand, aluminum bronze and beryllium copper should offer no difficulty to metallic arc welding, other than the formation of the oxide of aluminum or beryllium. This means, of course, a flux-coated electrode in order to avoid such difficulties.

Carbon Arc Welding

While no references are available on the carbon arc welding of either of the three groups of alloys, it is the reviewer's opinion that this method is the most practical at present available for the fusion welding of any of them.

The carbon arc welding of beryllium copper was briefly discussed in an article on resistance welding[28] as follows: "Beryllium copper may be carbon arc-welded if the operation be carried on at sufficient speed to forestall interference from the beryllium oxide. As an illustration, we found that by using a $^5/_{16}$-in. diameter carbon electrode with a current of 225 amperes and an arc length of $^1/_8$ to $^1/_4$ in. melting a $^5/_{32}$-in. diameter beryllium copper welding rod at approximately 1 in. of rod per inch of weld, we could obtain an excellent weld in $^1/_8$-in. thick beryllium copper sheet. The edges were given a 45° bevel to make a 90° single vee groove. The plates were clamped to a copper backer. No flux was required. The metal flowed together under the thin, tough, refractory skin of beryllium oxide with excellent soundness and strength. Welded strengths of 160,000 pounds per square inch have been obtained by this method in a peened and heat-treated sample which is, we believe, the highest strength obtained to date in a copper alloy fusion weld."

Resistance Welding

Cadmium copper finds a field of usefulness as resistance spot welding electrodes by reason of its good electrical conductivity and cold-work hardness. But as far as the reviewer can learn, no published information on applications of resistance welded sheets is available.

On the other hand, it has been demonstrated[28] that both aluminum bronze and beryllium copper can be resistant spot- and seam-welded. These metals are in fact given resistance weldability ratings second, only to the copper-silicon alloys and the copper-nickel alloys. Remarks on the resistance weldability of these alloys[28] are as follows:

"The resistance weldability of the 5% and the 8% aluminum bronzes is very good. The electrical conductivities are higher than for the silicon-copper alloys but they are still low enough to make it possible to weld such sheet with the usual welding transformers. The coating of aluminum oxide is not thick enough to seriously interfere with the resistance welding of the 5% alloy and even with the 8% alloy it gives little trouble.

"Starting with the clean metal, beryllium copper can be readily resistance welded. While placed in the order of second preference for resistance welding, further work may enable one to give it an "excellent" rating. Thus, there are a number of different conditions in which the metal can be welded, (1) in soft, annealed state, (2) in the cold-rolled condition, (3) after annealing and heat treating or (4) after cold rolling and heat treating. Similarly the assembly may be annealed and heat-treated after welding or simply given the precipitation heat. Ordinarily a resistance weld cannot be cold worked though such treatment would be beneficial." Strengths in resistance seam-welded beryllium copper sheets of 61,000 to 130,000 pounds per square inch are shown in the same article.

PART 8

Miscellaneous Welding Processes Involving Copper Alloys

The avidity and speed with which molten copper and

some of its alloys secure a high strength grip to the surfaces of various ferrous and non-ferrous metals gives us several brazing and bronze-welding processes of great importance. This importance is recognized by numerous writers who have published articles covering many applications of such processes.

Some of the names by which these various processes are known are as given below. The literature will be discussed under these several heads. Thus:

(*a*) *Brazing,* Hard Solder Brazing, Spelter Solder Brazing.*—Includes various types of copper-zinc solders as the 50 copper 50 zinc yellow brazing solder; another alloy in which about 10% of copper is replaced by an equivalent amount of nickel in order to make a white brazing solder; another alloy similar to the 50:50 alloy but with a small percentage of the copper replaced with an equivalent amount of tin to make a grayish, hard solder having a slightly lower melting point; and another darker alloy having a higher per cent of tin known frequently as "black button solder" and melting at a still lower temperature.

Occasionally, when the base metal is copper, the copper-phosphorus eutectic[22, 56] of approximately 93% copper 7% phosphorus is used.

(*b*) *Copper Brazing, Hydrogen Brazing, Controlled Atmosphere Brazing, Hyde Process.*—Usually the brazing solder is pure copper. However, this process overlaps (*a*) above, the spelter solders frequently being used instead of pure copper.

(*c*) *Bronze-Welding.*—The distinctive feature of this process lies more in the procedure than in the alloys used. The latter have characteristics somewhat similar to the yellow and white brazing solders mentioned in (*a*) but usually the zinc is 42% or less which makes the resulting weld metal ductile both hot and cold. Usually the strength is increased by small proportions of tin as in Tobin bronze or manganese and iron as in manganese bronze.

Bronze-welding is usually performed with an open vee groove, the bronze welding rod being melted by an oxy-acetylene torch and deposited therein on the base metal which is also heated by the welding torch but not usually melted.

The brazing alloy of (*a*) and (*b*) above, on the other hand, is invariably used as a thin film to bond or hard solder together overlapping surfaces. Usually this is done in a furnace, though sometimes the torch is used and not infrequently electric heat from carbon resistors and occasionally the carbon arc.

The above three processes (*a*), (*b*) and (*c*) are arranged roughly in chronological order. Brazing has been known for several centuries being brought into use shortly after the development of brass. "Copper brazing" or hydrogen brazing is credited to A. C. Hyde of Wolverhampton, England[140] by D. Richardson. The reviewer understands that this development started about 1915 though it was not employed in this country, to any great extent, until about 1928. Bronze-welding was known soon after the oxyacetylene torch was developed though its prominence as a commercial process did not begin until about 1922.

Base Metals

The base metals to which the above brazing and bronze welding methods are applicable included in general wrought iron, all of the various common and alloyed steels, gray, white and malleable cast irons, nearly all of the copper alloys and nearly all of the nickel alloys. Thus, if we except aluminum, zinc and lead, by far the greater part of the metals of commerce and industry can be joined by one or more of these brazing methods. Further details on the base metals will be brought out in the discussion on the various articles.

Method of Heating

The method of heating will depend on a great many factors. As, for instance, (*a*) size and location of base metal, (*b*) production of base metal pieces, (*c*) allowable capital outlay, (*d*) fit of base metal, (*e*) after use of brazed parts, etc.

Nearly all methods of heating are possible and have been used at one time or another. In the early days the blacksmith's forge or the wood or coal fired muffle furnace was used. This was followed by the gas fired muffle and later by the oil fired muffle and still more recently by the electric resistor furnace with the controlled atmosphere.

The well known torches as the compressed air, city gas, the atmospheric air-acetylene, the oxy-hydrogen and the oxyacetylene are applicable at one time or another.

The carbon arc torch in which the arc plays between two carbon electrodes and heat is taken by the work partly in form of radiant energy and partly from the lazily burning carbon gas. The Hanson torch[141] uses a direct current with an extra heavy carbon on the positive side while the Weco torch[142] uses an alternating current with two carbons of the same size. The former may be used with the usual D.C. arc welding generators while the latter is designed to be used with a step-down transformer connected to the usual 110 v. A.C. lighting circuit.

The atomic hydrogen flame is another A.C. arc torch[82] using tungsten electrodes with hydrogen gas for protecting the electrodes and work and transferring the arc heat to the work. With excess hydrogen, it could be used in the Hyde process of copper brazing.

A method of electric resistance brazing in which the low voltage (6 to 12 volts) high amperage alternating electric current from a step-down transformer is used to heat the work either by the resistance of the base metal itself or through contact with carbon resistors which become highly heated is described at some length by Gifford[143] also by Finkenstein & Brewster[144] and others. The former describes the making of electrical connections using silver solder melting at 725° C. (1337° F.) to 830° C. (1526° F.).

Reed, Unger & Gifford[145] describe the use of a similar apparatus using the carbon resistor tongs for making silver brazed electrical connections in conductors up to 2,000,000 C.M. in section. They also describe the making of the joint piece metal when the ends are too wide for a single heating of the resistor jaws. The heat flow from one brazed section to another is stopped by hack saw cuts separated by a distance approximately equal to the width of the carbon jaws.

S. Martin, Jr.[146] describes a similar electric brazing device elaborating on the correct set-up.

One advantage of this method of heating is that the parts to be joined *may be held together under pressure* from the beginning to the end of the brazing cycle. This is always a desirable condition in brazing especially when brazing sheet metal or electrical connections.

Finkenstein & Brewster[144] show that by the use of a 10 kva. transformer weighing 95 lb. developing 8, 10 and 12 volts in the brazing circuit, the joints between the amortisseur winding and the copper end rings of an electric motor can be brazed at a lower cost than by the use of an oxyacetylene torch.

Dip Brazing.—Another type of heating that has been used largely in the manufacture of small steel tanks for compressed air, etc., is that where the spelter solder is

* Silver brazing or silver soldering would also fall into this division. The silver alloys are, however, considered in another chapter and are not, therefore, included herewith.

melted in a metal container and the work dipped into the molten brazing solder.[140] The work must be cleaned and fluxed and preheated (usually) before being dipped. Thomas[147] recommended that the parts to be left free of brazing solder should be coated with a wash of molasses and graphite.

Brazing, Spelter Brazing

General.—Besides the various copper-zinc, copper-zinc-tin and copper-nickel-zinc "spelter solders" melting at temperatures from 782° C. (1440° F.) to 882° C. (1620° F.), the essentials[140] for correct brazing are (*a*) clean base metal surfaces, (*b*) close fit of base metal surfaces, (*c*) good brazing flux and (*d*) heat by one of the methods described above.

Preparation of Base Metal.—In general the metal must be chemically clean[140] if the strongest bond to the brazing alloy is to be obtained. It is particularly important to have the surfaces free of carbon in any form as the spelter solder will not take up carbon or stick to a surface coated therewith. Thus, carbon in the form of graphite from cast iron, temper carbon on malleable cast iron, oil, grease or carbonaceous matter on any surface must be removed. West[162] says, "Heavy scale and oil deposits are objectionable as the former leaves a spongy deposit which may prevent the copper from reaching the solid steel and the latter may carbonize which is an enemy to good brazing."

The surfaces must be overlapping or lying close together. In the case of the stranded cable, Martin[146] recommends that it be flattened as much as possible and cut the carbon resistors to fit the work. He suggests further that in making connections in insulated cables, the insulation may be wrapped in damp asbestos in order to prevent its charring in the brazing heat.

Thomas[147] suggests that parts may be "pinned together." Many times articles are lap-seamed, pressed together or clamped together for brazing.

A great deal of brazed high-carbon steel and brass tubing has been made by simply abutting the edges of the formed tubing, strewing same with powdered spelter solder and flux and heating in a furnace. In this case the base metal brass must have 75% or more of copper in order to avoid its fusion.

Flux.—Almost invariably, in spelter solder brazing, the cleaned surfaces are fluxed. Thomas[147] recommends as do Gifford,[143] Martin[146] and others that powdered *fused* borax be used rather than common borax as the water of crystallization in the latter causes it to foam up and expose the surface to be brazed. The borax should be sifted on *dry* or mixed into a paste with water-free alcohol. Martin[146] suggests the use of potassium acid fluoride mixture with fused borax and Leach[148] mentions mixtures of borax, boric acid, chlorides and alkaline bifluorides.

Bailey[149] gives an extended discussion of fluxes and chemicals used therefore but does not apply any particular one to brazing.

The methods of heating have been pointed out above. Where the quantity of work justifies it, a furnace is frequently used as in copper brazing described below. Where this condition does not obtain, a brazing or welding torch may be used or an electric brazing transformer and resistor jaws.

Brazing Metal.—As was pointed out above the spelter solders are made up of (*a*) copper and zinc, (*b*) copper, zinc and tin, (*c*) copper, zinc and nickel. Moreover, there is no sharp distinction between brazing with these alloys and copper-zinc-silver alloys, that is, the silver solders.

In addition the copper-phosphorous alloy[99] carrying various proportions of phosphorus from about 4 to 8% is used as a brazing alloy principally on copper base metal. When so used no flux is necessary as some of the phosphorus is volatilized which hot phosphorous vapor reduces any copper oxide which may be present, the resulting phosphorous oxide escaping as a gas. Various writers have described this copper-phosphorous hard solder and its application, thus. Glen[150] tells of replacing soft soldered connections with a 94% copper 6% phosphorous alloy melting at 700° C. (1292° F.). He states that it is very fluid when molten and self-fluxing on copper. He does not state the type of heat but the reviewer believes that it may have been the carbon arc.

Snyder[170] describes the use of the phosphorous-copper alloy with the oxyacetylene torch, the carbon arc and the electric carbon resistor brazing jaws. When using the latter method he suggests the insertion of a sheet of the phosphorous-copper solder between the two copper sheets to be joined and the latter heated by the carbon resistors to 800 to 835° C. "If too heavy a deposit of the solder is made," Snyder remarks, "it will be difficult to create a great enough loss of phosphorus to make the deposit tough."

Others[151, 152] describe the use of this alloy with 91.8% copper 8.2% phosphorus melting at 750° C. (1382° F.).

Copper Brazing, Hydrogen Brazing

General.—This type of brazed connection is relatively new. For the most part ordinary copper is used as the brazing solder—hence the name. The use of copper as the brazing material requires a high temperature, of the order of 1150° C. (2102° F.) which is another point of difference from the older brazing processes in which the temperature seldom exceeded 900° C. (1652° F.). A third distinctive feature is the use of a reducing atmosphere in the brazing furnace, usually one rich in hydrogen.

The copper functions very well as a solder where the base metal is *steel* because (*a*) molten copper "wets" clean steel readily, (*b*) because of the slight mutual solubility of copper and iron which fact enables a thin film of copper to flow, by capillary attraction, long distances and (*c*) because of the film of copper with an iron pick-up of possibly 3% has a strength nearly double that of the original copper.[153]

The copper braze applied at a relatively high temperature is undisturbed by a reheat to approximately 927° C. (1700° F.). Hence, the brazed steel articles may be normalized, heat-treated or fire enameled. This would not be possible with a spelter brazed or silver soldered article.

Again the hydrogen atmosphere makes it possible to use ordinary copper wire or scraps or even copper oxide as the brazing metal. All of the copper oxides are reduced to pure copper.

The hot hydrogen gas will also reduce iron oxides making the steel very nearly chemically clean. It will not, however, react with the graphite or carbon of grey or malleable cast irons. Hence, these metals should be freed of the surface carbon before being subjected to the copper brazing process.

West[162] cites the principal objections to hydrogen as (*a*) its high cost and (*b*) its tendency to decarbonize steel at 1150° C. (2102° F.).

Hydrogen Furnace.—The oxyacetylene or the oxy-hydrogen torch may be used as a means of heating and often is so used in the manufacture of kerosene and gas stove parts, metal furniture, etc. The copper brazing metal is reduced to pure copper in the torch atmosphere almost as completely as in the hydrogen furnace. However, most of the literature deals with furnace copper

brazing in a controlled atmosphere usually rich in hydrogen.

Webber in a series of articles[153–158] brings out some valuable and important features of the copper brazing process. He shows that the process is an economical and satisfactory method of fabricating refrigerator evaporators, floats, check valves, pistons, bottom plates, light tanks, golf club shafts, steam turbine diaphragms and nozzles, automobile camshafts, composite dies, tungsten carbide tools, sewing machine assemblies, business machine parts, etc. Failures of the six (6) different copper brazes in each of one and one-half million refrigerators are practically unknown.

He shows that the parts to be brazed should be fitted together with a tolerance not greater than plus 0.003 in. as with greater tolerance the capillary attraction may not be sufficient to draw the molten copper into the joint.[153] He remarks that "Joints cannot be made too tight. Copper will flow into the tightest joint. Press fits are desirable."

Rude[159] mentions the assemblying of parts for copper brazing by press fits or spot welding.

Webber found the strength of the copper brazed joint in steel to be 58,000 psi with a yield point of 36,000 psi. On another test a high-carbon steel broke in the copper brazed joint at 70,000 psi. In another article[157] he says "40,000 to 45,000 psi, in low-carbon steel copper brazed is a reasonable strength to expect." He reports one manufacturer as being able to increase the strength of one assembly 287% by copper brazing which strong connection enabled him to lighten the part and thereby reduce the inertia.

Possible Base Metals.—Webber[157] says that steels carrying more than 1% chromium need a flux to make the copper wet it. Silicon and aluminum also form oxides with impurities in the furnace gases which cannot be reduced by hydrogen. He reports that nickel, silver, bronze, deoxidized copper and brass can be brazed but that solder will penetrate only a short distance on account of going into solution in the base metal. Brass must be heated quickly to avoid vaporization of the zinc.

Robiette[164] notes that where high nickel alloys are copper brazed, the copper has a tendency to alloy with the nickel. The resulting alloy having a higher melting point than copper will freeze on surface instead of running into the joint as it does with steel.

Webber[157] notes that copper at 2100° F. will dissolve about 4% of iron while iron will dissolve about 8% of copper at the same temperature, but on cooling to room temperature, the two metals precipitate leaving a film of copper with only a fractional per cent of iron in solution.

Handicaps in Copper Brazing.—While it is possible to do hydrogen copper brazing with an oxy-hydrogen or oxyacetylene torch and such torches are actually being used in manufacturing, mass production demands a furnace good for 1150° C. (2102° F.) with a controlled atmosphere. Such a furnace calls for an appreciable capital outlay and the operating charges for heat and atmosphere are relatively high. In general the furnace must be proportioned for the work it is to handle—designed for an economic volume. While not explicitly mentioned in the literature, the reviewer gathers that the electric resistor heating is almost a necessity.

Webber[155] mentions an electrically heated, semi-continuous, tunnel-type furnace with belt conveyor. Rude[159] and Anderson[160] speak of the large electrically heated, hydrogen atmosphere tube furnace of the Bundy Tube Company as a "zeppelin" type furnace. They remark that it is a large investment. West[162, 163] says that the conveyor type furnace is satisfactory at furnace temperature up to 982° C. (1800° F.) while pusher type should be used up to 1150° C. (2102° F.).

West[162, 163] says the process is held back by cost of hydrogen atmosphere which he quotes at $3.00 to $10.00 per 1000 cubic feet. "City gas," he says, "can be converted to yield a satisfactory proportion of hydrogen at a cost of 20 to 30 cents per 1000 cubic feet."

Robiette[164] addressing a British audience says "Generally speaking, three to six pounds of work are copper brazed in an electrically heated hydrogen furnace for each kwh. of electrical energy consumed." He quotes the cost of hydrogen in London at fifty to sixty shillings per 1000 feet and dry ammonia cracked at 560° C. over a catalyst to yield 75% hydrogen and 25% nitrogen at twenty-four shillings per 1000 cubic feet.

He says further that it is possible to produce from butane or coal gas an inexpensive mixture of 12 to 20% hydrogen, 8% CO_2 and 12% CO which is feebly combustible, non-explosive and non-decarburizing. The cost is one shilling for the mixture from coal gas and two for the mixture from butane per 1000 cubic feet.

Robiette[164] adds that approximately 750 cubic feet of gas per 300 lb. of work per hour are required.

Sass[165] describes a pot type of furnace with a lid sealed with cement dust, filled with hydrogen at a pressure of 8 in. of water and used to copper braze steel plates to cast steel engine heads of 1200° C. (2192° F.). Requires eight to nine hours to heat, one-quarter hour at temperature and forty hours to cool off.

Webber[166] describes at some length the formation of "electrolene" gas starting with city gas and steam. At 2000° F. the two original gases are broken down to form the electrolene which has 67% of hydrogen and 25% of CO with 8% of other gases. The cost is approximately $1.00 for 1000 cubic feet of electrolene which is used as a furnace atmosphere.

Brazing Alloys.—West[162, 163] cites the use of silver solders at a furnace temperature of 871° C. (1600° F.) and brass alloys as brazing materials at a furnace temperature of 949° C. (1740° F). He sometimes finds minute pores in parts brazed with brass due to the evaporation of zinc. Hence, it is not to be recommended for gas tight refrigerator parts. Copper-brazed joints are better.

Sass[165] describes the use of an alloy of copper, nickel and zinc melting at 1150° C. (2102° F.) for brazing diesel engine heads. In this case the high melting point is desired on account of service temperature the work must endure later. He remarks that the nickel and zinc are added to make the alloy stronger and less fluid than pure copper.

Most of the authors prefer ordinary electrolytic copper wire or copper particles applied as a paste. Sometimes the copper is applied as a copper paste to the steel.[162]

Where torch brazing is resorted to as in the case of the manufacture of metal furniture, electrolytic copper, deoxidized copper or a yellow bronze may be used.

After-Treatment.—Usually no after-treatment is necessary for copper brazed products (as a part of the brazing process itself). The parts are chemically clean. Usually there is no flux to be removed, and the parts may be heat-treated, painted or fire enameled, plated, etc.

Bronze-Welding

As was pointed out in the general discussion at the beginning of Part 8, bronze-welding is relatively new compared with spelter solder brazing. By reason of its ease of application, universality, good strength, ductility and wear resistance of the bronze welded joint or surface the process has grown to be one of great economic im-

portance, both in the manufacturing field and in the repair and maintenance of machinery.

One of the best definitions of bronze-welding is given in Oxy-Acetylene Tips in 1930.[181] "Bronze-welding should not be confused with brazing, although the process is somewhat similar. Brazing, which metal-workers have employed for a long time, consists in heating the metal parts to be joined hot enough to melt a brass alloy called 'spelter,' usually supplied in powder or chip form, so that it flows into the fluxed joint. The use of the oxy-acetylene flame to accomplish brazing led to the development of the modern art of bronze-welding. In bronze-welding, the bronze is supplied in the form of welding rod and oxyacetylene flame serves to heat the base metal to the proper temperature as well as to melt the welding rod. The operator is thus enabled to control the work accurately at all times. Although not a fusion weld, in many cases a bronze-weld made with a high grade bronze-welding rod and good flux produces a joint comparable in strength and reliability to a fusion weld."

An earlier paper[187] read by W. C. Swift at the International Acetylene Association meeting in 1926 shows the term "bronze-welding" to have general acceptance at that time. He describes it as "the process of making a joint by heating clean weld areas of the base metals to a red heat only and then melting the fluxed end of a bronze rod onto the heated areas and continuing until the 'vee' is filled or a bead of correct width and height is deposited." He cites numerous applications.

A great deal of literature is available of which a part will be quoted to illustrate the various steps in the bronze-welding procedure. Some of the data from Part 3 and Part 4 are applicable to bronze-welding.

Historical.—With the advent of the oxyacetylene torch into this country about 1905, metal workers hailed the new process enthusiastically saying that now we have at last a method of making homogeneous ("autogeneous," they called it) connections in metals. Their enthusiasm dimmed their perception somewhat for it has been only within the last decade that metal workers generally have accepted the fact that for the best welds, the welding wire should not be the same as the base metal. As Granjon[173] remarked in 1930, "Earlier advocates of the oxyacetylene welding process adhered strictly to the literal sense of the term 'autogeneous' when carrying out welds. Welds can be 'heterogeneous' and yet give excellent results."

The earlier view held back the general acceptance of the yellow welding bronzes for nearly a score of years. Thus we note in a 1926 issue of Oxy-Acetylene Tips[167] the following statement, "As recently as four years ago (1922), bronze-welding—was practically unknown outside of a few large railroad shops and industrial plants."

Fortunately, when the welding operators were looking around for a welding rod to join dissimilar base metals as cast iron and steel or copper and steel, an excellent welding bronze was already available in Tobin's bronze[168] having been developed in 1884 and described by Thurston[89] in 1897. Parson's manganese bronze developed about the same time as Tobin bronze[89] was also found to be a satisfactory welding bronze. It was somewhat harder and less ductile than the Tobin bronze.

One of the early references to bronze-welding is the description given in 1922 by Kinsey[169] of a Tobin bronze weld joining a steel angle flange to a copper cylinder. The same writer[174] amplifies on various phases of non-ferrous welding in 1924. Dawson[103] in a lecture to the International Acetylene Association, October 1921, remarked, "the most popular bronze-welding rods are the manganese and Tobin bronzes." Bronze-welding proved to be a very satisfactory method of joining cast-iron pipe as was demonstrated in the November 1924 issue of *Oxy-Acetylene Tips*.

Mechanism of Bronze Welds.—Comparatively little is known concerning the why's and wherefore's of the action that enables the yellow bronzes to take such a powerful grip on so many different unfused base metals. Jackman[171] has given perhaps the best explanation of the bond between the bronze weld metal and the base metal. He says, "A normal weld shows an intimate bond between the Tobin, manganese or nickel welding bronze and wrought iron or cast iron base metals but little penetration. High temperatures of 900° C. (1652° F.), or thereabouts, for long periods increased diffusion of bronze into steel slightly . . . . . Intergranular penetration by the bronze as an alloy, and not by any one constituent, accounts for only part of strength of bond. There is a small amount of surface alloying which accounts for the greater part of the strength."

A publication[176] of the International Acetylene Association of 1935 has a very good summary of this action stated as follows: "It has been found that three distinct forces act to maintain adherence of the bronze to the base metal."

"(*1*) *Tinning.*—A clean metal surface will receive a thin film of a molten metal of low surface tension in the same manner that water will spread over a clean glass plate. The bond thus produced depends on the action of molecular forces at the bronze-base metal interface.

"(*2*) *Alloying.*—A diffusion of the bronze constituents, copper, zinc, tin and others, into the base metal, and a corresponding diffusion of the base metal constituents into the bronze takes place in a narrow zone at the bronze base metal interface. The base metal crystals, at the interface, when examined under the microscope, show that there is a diffusion of the bronze constituents into the crystal grains, and an alloying of the bronze metals with the base metal.

"(*3*) *Intergranular Penetration.*—The action of the molten bronze on the base metal surface opens up the crystal grain structure of the base metal surface and allows the bronze to penetrate the base metal along the grain boundaries.

"The action of these forces, in particular the alloying and intergranular penetration effects, accounts for the very great strength of the bronze-base metal bond, which can be produced without surface fusion of the base metal."

The Committee were referring particularly to cast iron as the base metal in the above. The reviewer believes, however, that the same set of forces would apply to any other base metal but with a different emphasis on one or the other. Thus paragraph (*2*) above would apply particularly to copper alloys, bronze-welded.

The reviewer[172] showed by inference that this alloying action or penetration is more rapid between grains than through grains of steel and that it is accelerated greatly by tension in the base metal. It was demonstrated further that a steel base metal should not be bronze-welded when it is carrying a tensile stress greater than 2000 psi. With higher tensile stresses the penetration of the molten bronze is extremely rapid. In the early days of bronze-welding many futile attempts were made to bronze weld cracks in the relatively thin steel automobile frames without removing the load therefrom.

When the base metal is copper or a copper alloy, the surface alloying action is uniform (not intergranular) and rapid. Hence, the bond approaches the strength of the weaker metal. In welding muntz metal, Tobin bronze or manganese bronze, the weld becomes a straight fusion weld.

Interference with the Bond.—Numerous writers have

called attention to the various substances that will diminish the strength of the bond of the bronze to base metal. Any oxide is bad except for instance those which are in the nature of fluxes as the boron oxide, sodium oxide, potassium oxide and sometimes silicon oxide. However, the more common oxides of copper, zinc, tin, iron and manganese are easily dissolved in flux and offer no obstacle to bronze-welding. The oxides of aluminum, chromium and nickel are difficult to flux and lower the strength of the bond. In the case of nickel, however, no difficulty is experienced in bronze-welding as, starting with the metal chemically clean, the usual fluxes will prevent the formation of nickel oxides at bronze-welding temperatures.

More serious perhaps than any of the oxides is the interference offered by carbon, in the form of graphite, temper carbon (in malleable cast iron) or carbonized oil, etc. No flux will dissolve carbon, and bronze will not take it into solution or adhere to it. Hence, the graphite smear must be removed from cast iron and malleable cast iron and burnt oil, etc., removed as well before welding bronze is deposited.

Ames[180] gives us the benefit of his experience as follows, "In many shops where a grinding wheel is handy, an operator will bevel the edges to be brazed (bronze-welded) on the wheel. The prepared job looks perfect for brazing but . . . . . the black graphite has spread a thin film over the cast iron or malleable casting . . . . . which spells doom for many brazers.

"After grinding, remove the fooling film with a bastard file. Make several deep cuts on an angle using the corner of the file . . . . . to increase the gripping surface for the bronze. . . . . Whenever possible, use a sandblast to drive out the soft graphite spots and thus secure a better bonding surface."

Carbon dissolved in iron does not interfere with the bronze bond. Thus, high-carbon steel and white cast iron may be bronze-welded without difficulty. *Industry & Welding*[179] cites the successful bronze-welding of a molybdenum-bearing, white cast iron centrifugal pump housing which had broken in the pumping of sand from the river to a railway fill at East St. Louis. The broken flange was replaced by a steel flange which was bronze-welded to the white iron housing. The ductility of the bronze saved the housing from cracking and joined the dissimilar metals (steel and white iron) with ample strength for continued service.

As was pointed out by the reviewer[176] the free carbon can be removed from the surface of cast and malleable iron (*a*) by sand blasting, (*b*) by annealing in a mildly oxidizing atmosphere which burns off the carbon before the iron is seriously oxidized, (*c*) by "searing" with the torch (a heating to cherry redness and exposing to the air) or (*d*) by the use of an oxidizing agent in the "tinning" flux.

The "searing" method of eliminating graphite on cast-iron surfaces was developed in 1928 or earlier, *Oxy-Acetylene Tips* of December 1928 says, "The searing mentioned (as a preliminary to bronze-welding cast-iron pipe) is a new feature of, bronze-welded cast-iron pipe installations. Some time ago, it was found in investigations carried out on experimental installations of bronze-welded pipe, that the machining of a bevelled edge for welding had the effect of exposing the small flakes of graphite which are typical of cast-iron structure.

"Acting on the results of these investigations, it has since then been the practice on all installations of bronze-welded cast-iron pipe to subject the ends of the pipe, after bevelling, to an oxyacetylene flame until they become a dull red, after which they are allowed to cool. This burns up the graphite flakes. . . . . The bronze 'tins' quickly and easily on the seared edges."

Oxy-Acetylene Tips[178] in a very complete article on all phases of oxyacetylene bronze-welding recommends the use of a strong oxidizing agent like potassium chlorate as a "tinning" flux only on cast iron which is found difficult to tin. If the trouble is due to graphite, the oxygen freed from the chlorate by the red hot iron will combine with the carbon, after which the bronze can be applied with the usual brazing flux. In another article[184] they say, "If a trial application of the fluxed bronze rod on the cast iron heated to a proper temperature shows that the surface resists 'tinning,' prefluxing with a strongly oxidizing agent as potassium chlorate on the part of the cut heated to a red color ahead of the weld puddle will correct the difficulty. And as soon as the foaming of the chlorate ceases, the normal welding operation can proceed with the usual flux. This oxidizing of the cast-iron surface leaves a normal gray iron surface to work on." *Journal de le Soudure*[186] also calls attention to this procedure. The reviewer has found manganese dioxide used in a similar manner.

Kinzel & Lytle[93] say that "chromium and aluminum oxides call for more active fluxes" but do not specify reagents.

Oxyacetylene Bronze-Welding

All of the above general discussion on bronze-welding is applicable to oxyacetylene bronze-welding. In fact by far the greater part of bronze-welding where a high zinc-welding rod is used is done with the oxyacetylene torch. The direct arc vaporizes too much zinc from the weld metal to make its application satisfactory.

Preparation of the Base Metal.—The principal difference between the preparation for bronze-welding and brazing, hereinbefore discussed, is that in general in the former no great reliance is placed on the cementing action of the bronze between two closely contacting surfaces whereas in the usual braze the spelter solder or copper is invariably so used. This being the case, the open vee is commonly used for butt joints[181, 182, 140] as illustrated in Fig. 8.

In some cases, where there is a possibility that the bond of weld metal to base metal is apt to be weaker than either the weld metal or base metal as is sometimes the case with improperly prepared cast iron, the bonding area is increased.

The "shear-vee" illustrated in Fig. 8 (*b*) is credited by W. C. Swift[140] to T. W. Green. It is decribed at some length in the *Plumbers Trade Journal*[183] in which the optimum dimensions of the vee are given for all thicknesses of cast-iron pipe. The "wide-angle vee," Fig. 8 (*c*), accomplishes the same purpose.[176] The elastic, bronze, stop-leak joint illustrated in Fig. 8 (*d*) is intended merely to stop leaks in cast-iron boiler sections for low-pressure boilers. Full details of the procedure employed are given[184] in the article.

The preparation of the surface has already been discussed in the foregoing.

Preheat.—No preheat is necessary in ductile materials as the wrought copper alloys, steel, nickel alloys, etc. Whether preheat is necessary in the case of cast irons, cast bronze and such depends on the conditions of each case. In reviewing the literature we have found many cases cited where a preheat is regarded as desirable as in the bronze-welding of cast-iron boiler sections[184] where a preheat temperature of 500° F. is recommended. In bronze-welding heavy sections of cast iron as frames for large presses, rock crushers, machinery frames, etc., "it is frequently necessary to have gas torch preheating flames constantly playing on the cast iron."[181]

Patterson[185] shows that in welding intricate cast-iron

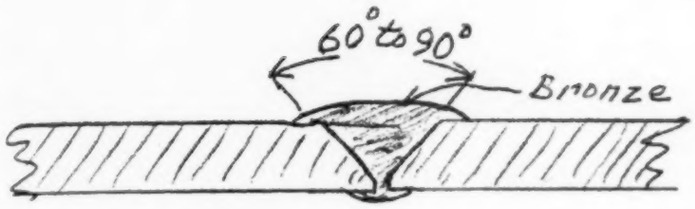

Fig. 8(*a*)—Bronze Weld on Steel, Cast Iron, Copper Alloy, etc.—Usual Vee

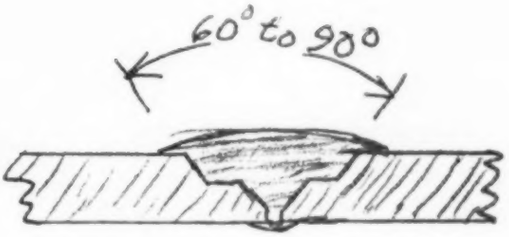

Fig. 8(*b*)—Bronze Weld on Cast Iron—Shear Vee

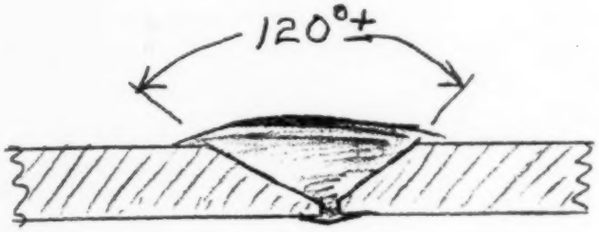

Fig. 8(*c*)—Bronze Weld on Cast Iron—Wide Angle Vee

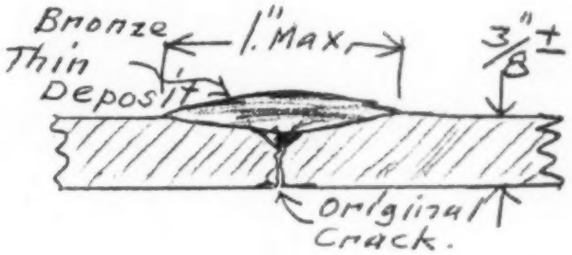

Fig. 8(*d*)—Bronze Weld on Cast Iron, Low-Pressure Boiler—Elastic, Stop-Leak Weld

parts as the cylinders and steam chests of locomotives, a good black temperature preheat with charcoal or gas followed by a local preheat to about 1100° F. obtained with torches is desirable before the bronze is applied with the welding torch.

However, all writers are agreed that bronze-welding can proceed with a lower preheat than is the case when cast iron is to be welded with cast iron, for instance. This situation summed up in an International Acetylene Association report[175] as follows: "One of the principal advantages of bronze-welding over cast-iron welding rests in the relatively small amount of preheat necessary to prevent distortion of the work from the bronze-welding heat. Since, in bronze-welding, the base metal surface is only brought to a tinning heat and is not fused, the transfer of heat from the weld to the work is very much lower in bronze-welding than in cast-iron welding. The advantages in time, labor and material savings involved in this difference between bronze-welding and welding processes requiring surface fusion of the weld face, will be immediately apparent to those familiar with the elaborate and time consuming preparations attending the cast-iron welding of all but the simplest of cast-iron shapes."

Bronze-Welding Rods.—The welding rods used for oxyacetylene bronze-welding are invariably of the high zinc-bronze type. Because of this high zinc content, they are quite unsuited for either metallic or carbon arc welding.

Tobin bronze and manganese bronze have already been mentioned in the foregoing. They are further described in several articles.[97, 140]

Lytle[188] notes that of the straight copper-zinc alloys, the 60 copper 40 zinc alloy gives the best combination of strength, hot and cold ductility and quick freezing. He notes, however, that the straight 60:40 alloy "fumes and boils and is sluggish in tinning ahead of the welding heat. Tin hardens and strengthens the metal but its greatest effect (in welding) is on the flowing qualities. Tin aids in obtaining good molecular union. Lead increases porosity."

"In manganese bronze," Lytle[188] continues, "containing tin, iron and manganese, tin and iron cause formation of hard particles of delta constituent evenly distributed throughout the bronze matrix. These hard particles give manganese bronze good wear resistance."

"Silicon," Lytle finds, "functions as a deoxidizer removing tin, copper and zinc oxides and forming silicon dioxide which floats readily to the surface; keeps gases in solution resulting in greater density and toughness of weld metal; and keeps down zinc volatilization by film of molten slag."

Kinzel & Lytle[93] in a later paper give a more comprehensive survey of bronze-welding. They say, "Phosphorus is a good deoxidizer for bronze and brass but cannot be used for bronze-welding rods which are to be employed on cast iron or steel on account of the formation of a brittle film of iron phosphide between the bronze weld metal and the iron base metal."

"Nickel has very little effect on the flowing qualities unless present in considerable more than 1%. Used for whitening the bronze (nickel silver)."

"Silver is used in copper to disperse the copper oxide (sic) but is not used in the average bronze-welding rod on account of its cost."

Lytle[189] patented the use of silicon in a welding bronze in 1929. The beneficial qualities claimed are as stated above.[188] Kinzel & Lytle[93] say that 0.12% of silicon is sufficient to obtain the optimum results in a bronze-welding rod. They show excellent strength results of 52,300 psi in a bronze-welded steel sample and as high as 63,500 psi for a sample made up entirely of bronze weld metal.

Keel[182] made bronze welds on steel and cast iron using (*a*) the 50:50 copper-zinc brazing alloy melting 820 to 875° C.; (*b*) Tobin bronze melting 875° C.; (*c*) European bronze-welding rods with 60% copper, 40%

zinc and 0.2 to 0.4% silicon; (*d*) newer bronze-welding rods 45 to 50% copper, 10 to 15% nickel, 40% zinc melting at 910° C. He obtained tensile strengths of steel welded with these various bronzes from 35,000 to 70,000 psi. The lower values were obtained with those rods highest in silicon, the failure taking place in the bronze-steel interface. The higher values were obtained with the rod (*d*) having the 10 to 15% of nickel.

Welding Procedure.—Most of the details of the bronze-welding procedure are given in the foregoing. By far the greater majority of the writers recommend a neutral flame. Kinzel & Lytle[93] express preference for a slightly oxidizing flame.

After-Treatment.—In no case is any after-treatment recommended. Slow cooling is generally recommended.

Flux.—As was noted before, there is little scientific data available on fluxes. There is, however, general agreement in the literature that a good brazing flux is necessary in bronze-welding. The reviewer has found that these fluxes are commonly made up of fused powdered borax and boric acid in various proportions. Exact formulas have not been published.

As is noted in the foregoing, potassium chlorate or manganese dioxide are sometimes used on cast iron as a tinning flux only. They give up oxygen readily which at red heat burns off the graphite from the base metal surface.

Bailey[149] in 1933 quotes several earlier authors and gives some valuable data on oxides and their relative fluxability. He says, "Boric oxide forms fluid borate slags with certain other metallic oxides, but in many cases, notably aluminum and tin, it has been stated by Burgess & Holt (*Proc. Chem. Soc.*, **19,** 221 (1903)) that the oxide is quite insoluble in fused boric oxide. It cannot, therefore, be assumed that the addition or formation of boric oxide will result in a fluxing of other oxides present, and each case must be presented on its merits."

"Fluxes most valuable as solvents for metallic oxides and as protective covers are compounds of boron. . . . . Zinc oxide is soluble in molten sodium carbonate but tin oxide is not. . . . . Common green glass is an excellent solvent for zinc oxide and as a cover for molten brass (*Ebell, Dingler's Journal*, **64,** (220) 155). . . . . The action of common salt is obscure."

The writers agree generally in the use of dry flux rather than in paste form. It is sprinkled on the work or picked up by the hot end of the bronze-welding rod.

Applications of Bronze-Welding.—Considerable space is given in the literature to citing and describing particular applications of the process. Only sufficient of the illustrations are mentioned here to show the broad scope of the bronze-welding process. Thus, *Industry & Welding*[179] describes the repairing by bronze-welding of:

"Bliss No. 4 toggle press frame for drawing barrel heads, cooking utensils, etc.

"300 pound cast steel gear wheel. Two broken spokes bronze-welded in four (4) hours."

Endicott[190] tells of bronze-welding automotive engine blocks, large machinery as rock crushers, Diesel engine frames, locomotives, foundry flasks, cast-iron pipe, malleable iron castings, steel furniture, ornamental iron, galvanized iron, copper, silicon deoxidized copper, copper, brass and bronze piping, worn surfaces.

Oxy-Acetylene Tips[193, 194, 167] mentions cast-iron refinery oil pumps, housings, brewery equipment, motor truck, blower engine cylinder, gears, gear teeth, etc., repairs with bronze-welding.

Farr[195] gives details on the bronze-welding of cast steel locomotive frames using the double vee vertical bronze weld, cracks in cast-iron steam locomotive cylinders bronze-welded with a saving of $2500.00 in one instance, locomotive piston heads and driving boxes built up with wear resisting manganese bronze.

Miller[197] describes the rebuilding of eroded centrifugal pump impeller blades with bronze, also pump wearing rings, repair of a broken cross head, pump gears, casings, etc.

Aitchison[198] tells of restoring cast-iron plungers for boiler feed water pumps, bull rings, industrial locomotives, equipment for coal mines, foundries, brick manufacturies, etc. Ketchbaw[201] gives valuable detail on the bronze-welding of locomotive frames and cylinders.

Some special applications are worth more detailed mention, thus:

(*a*) *Cast-Iron Pipe.—Oxy-Acetylene Tips* gives the following data.[191,192,177,183] "The pipe is bronze-welded at the foundry into 24 ft. or greater lengths in order to reduce the number of field welds. Expansion joints should be put in the line 72 ft. to 96 ft. apart. Use a vee or shear-vee butt joint. Align the sections carefully. Tack joints in two or three places with the bronze making a small tack in the bottom of the vee. Heat the pipe for some distance each way from the joint from the starting point and finally concentrate on a section near the top bringing it to a "tinning" heat and, having heated and fluxed the welding rod, bring same into the flame approximately in line with the vee. Complete the weld in one pass being careful not to flow the bronze ahead of the tinning heat." The articles also describe the bronze-welding of 36-in. diameter cast-iron pipe. It was necessary to tack the latter in six (6) places before starting the continuous weld.

(*b*) *Galvanized Iron.*—The *Oxy-Acetylene Tips*[178] describes in detail the bronze-welding of galvanized steel or iron without serious loss of the protective zinc coating. One article[181] details the bronze-welding of galvanized iron piping for a salt water swimming pool installation. The bronze-welded points were found to be as completely corrosion-resisting as the main pipe lines. Another article[196] gives the procedure used in the production of acetylene generators from galvanized sheet iron by bronze-welding. Reinhard[199] describes in detail the economy of manufacture and the corrosion-resistance of the product obtained by joining the galvanized sheets and fittings of an acetylene generator by bronze-welding. Meslier[200] shows that in bronze-welding of galvanized iron, the zinc coating is fused but not destroyed by vaporization.

(*c*) *Bronze-Welding Brass and Copper Pipes.*—Some of the data has been reviewed in Part 3 in which bronze-welded pipe connections are described. However, Fetherston[202] gives particularly interesting detail on the making up of copper pipe from copper sheet by bronze-welding and installing same on a paper pulp mill also by bronze-welding. No difficulty from corrosion was experienced in the operation of the bronze seamed and butt-welded copper pipe in handling the corrosive liquors in the pulp mill.

Mashl[62, 210] describes the bronze-welding of thin-walled deoxidized copper tubes into a $\frac{1}{2}$-in. thick flat copper head by the use of a yellow welding bronze. The head and tube ends were preheated with charcoal and the entire flat head flooded with bronze, using the oxyacetylene torch, which bronze-welded the tubes to the head.

(*d*) *Rebuilding Valve Seats.—Oxy-Acetylene Tips*[203] gives an exact procedure for rebuilding burnt and worn valve seats in automotive engines stating that the application of No. 25 M bronze-welding rod increases the life of the valve seat 25 to 75% before a regrinding becomes necessary. The bronze is deposited on the valve seat by the usual bronze-welding methods. They say

further, "Bronze, unlike cast iron, does not retain and hold carbon that is being ejected from the combustion chamber. Particles of carbon are blown clear, they do not adhere to the bronze, and the continual action of the value of the seat does not grind down the seat nearly as rapidly as if there are specks of carbon to act as an abrasive adhering to it."

They recommend machining out a round-bottomed depression in the valve seats, start the preheat and the bronze-welding at one end of a multi-cylinder block and preheat and weld all seats in order from one end of the block to the other. After depositing the bronze, rough machine, peen the bronze to harden it, finish machine and grind valve into finished bronze seat.

Bronze-Welding by the Electric Arc

As explained in the foregoing, the high zinc bronzes are not satisfactory for welding with the metallic or carbon arc. There are, however, other copper alloys which work very well with the arc. Thus, the phosphor bronzes are readily deposited—particularly with the carbon arc—as weld metals and the copper-silicon, copper-nickel, copper-aluminum and copper-beryllium alloys also as described in the foregoing Parts 3 to 7 inclusive.

Bronze Wear Resisting Surfaces

The use of the carbon or metallic arc for making deposits of the wear resisting bronzes on cast iron, cast or wrought steel is a process of considerable economic importance which is growing rapidly in this country. The railroads especially are bronze-facing by arc (and sometimes oxyacetylene) welding methods many of their locomotive wearing surfaces. However, there is very little literature on the subject. It is in fact almost nonexistent owing principally to the newness of the application.

One excellent published study of bronze surfacing is that by Jennings[204] in which he describes the surfacing of steel rocker rings which carry the motor brushes on an electric locomotive. He describes the use of phosphor-bronze rods as metallic arc electrodes. Since he does not desire to bring up globules of steel into the bronze, Jennings cautions one that the bronze must be "brazed"* onto the steel rather than fusion welded. He accomplishes this effect by melting down a small pool of bronze on the steel base metal and from that starting point, allows the arc to play only on the weld metal which is superheated thereby sufficiently to develop a satisfactory bronze weld to the base metal.

Jennings found by actual trials that an arc length corresponding to a voltage drop of 35 to 40 with a D.C. current value of 90 to 110 amperes on a $^1/_8$-in. diameter bare phosphor bronze wire, 120 to 140 amperes on a $^5/_{32}$-in diameter and 140 to 180 amperes on $^3/_{16}$-in. diameter wire, would yield sound weld metal, securely welded to the base metal. While not explicitly stated, it is the reviewer's understanding that they used a phosphor bronze carrying 5% tin with approximately 0.30% phosphorus, the remainder copper.

Smith[205] describes the use of a mineral-coated copper electrode which he advocates as a metallic arc electrode for welding cast iron. The coating carries an element which is intended to alloy with and strengthen the weld metal. The weld is ductile. The rod may be used at low current values on relatively thin sheets. Also usable on galvanized steel sheets.

Welded Rail Bonds.—Another application of somewhat narrower scope is the welding of current-carrying conductors to steel railway rails for power or signal circuits. This application was developed rather early. It was well established when the committee[60] of the American Bureau of Welding reviewed the literature on the arc welding of copper alloys up to March 1923.

Febrey[206] discussed in 1923 the various types of rail bonds as mechanical, soldered, brazed, metallic and carbon-arc welded. He pronounced the latter type, welded directly to rail head most satisfactory. He used a copper rod having 0.25% silicon.

Gailor[207] stated in 1923 that within the previous 10 years the electric arc welding of rail bonds had been accomplished satisfactorily. He described the use of a carbon mold to hold the weld metal against the rail head until it solidified. He also used a combination mold of copper, steel and carbon which he claimed to be better than plain carbon. He used the carbon molds for 40 to 50 welds before they were used up. He obtained equally good results with carbon arc and metallic arc. He preferred a ferrule on the end of a stranded bond in order to facilitate the welding operation and improve fatigue life of the strand.

Arc Welding Galvanized Iron.—Several authors have shown that arc welding is a practical means of joining galvanized iron. *Industry & Welding*[208] gives a simple and apparently very effective method of carbon arc welding galvanized iron sheets without injuring the zinc protecting coating. A bare phosphor-bronze rod is laid on the butt joint of the galvanized sheets which are clamped to a smooth steel backer. A small diameter ($^1/_{32}$- or $^1/_4$-in) carbon electrode set for a low current of 35 to 45 amperes is struck on the phosphor-bronze which is melted in one continuous pass. Strong neat butt or fillet joints are made with no loss of zinc.

Ericson[209] attained equally satisfactory results with a procedure similar to the above except that he used an Everdur (silicon-maganese-copper) welding rod coated with a composition of tin. The tin alloyed with the Everdur and the zinc coating as the rod was melted with the carbon arc.

Conclusion

The welding of copper alloys is by no means a finished art. Changes in the alloys in order to make them more suitable for some particular service are constantly taking place. This frequently means a change in the welding procedure. As will be seen from Part 1, small fractions of a per cent of some added elements have a profound effect on the welding properties. These effects are far from being completely established. It is quite possible that at some future time, systematic research will disclose a deoxidized copper in which welds can be made stronger, sounder and more ductile than any untreated welds of today.

Again it may well be that heavily, fluxed-coated or slag-coated copper alloy metallic arc electrodes may show as great an improvement in copper alloy welds as has been the case in steel welds. Several commercial firms have made a start in this direction but little if anything has appeared on the subject in the literature.

Some of the fundamental data that would be enlightening, such as the coefficients of expansion (contraction) for high temperatures approximating the melting points of the alloys and the oxide dissolving power of various fluxes, are not available in the literature. It is not unthinkable that some chemical may be found which will dissolve aluminum or beryllium oxide as freely as the borax fluxes do the copper oxide thus making aluminum bronze and beryllium copper readily gas weldable.

Some welding processes are unduly expensive by reason of the large labor factor as for instance those in the common oxygen bearing copper which require cold

* Jennings uses the term "braze" to describe a bronze weld in which the base metal is not fused.

peening and annealing or hot forging. Again, some processes are available to but few on account of the high capital outlay for equipment as for instance that required for resistance welding the high conductivity copper alloys or that needed for copper brazing in hydrogen.

Again, bronze welding has not been accorded the place in our industry that is warranted by its economy and universality. One might say that if a bronze weld will successfully withstand (as so many hundreds of bronze welds have withstood) the grueling service that will crack an 8-in. x 8-in. cast steel locomotive frame, it will endure anything. Yet we find one of the world's leading text-books on welding giving only one short paragraph to bronze-welding. Many operators have still to overcome an unwarranted antipathy to a weld of a different color from the base metal.

Finally, wear-resisting bronze surfacing is just beginning to be appreciated as an economical means of extending the life of a costly piece of equipment.

Hence, January 1, 1936 finds us with a good start in the welding of copper alloys but with a great deal still to be done.

Acknowledgment

The foregoing Review is a contribution to the Copper Industry by the American Brass Company and the AMERICAN WELDING SOCIETY under the auspices of the Engineering Foundation, Welding Research Committee. The reviewer is very glad to acknowledge with a deep appreciation the unfailing cooperation of the management of the industrial concern and the editorial staff of the technical societies.

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Obituary

Dr. Alfred D. Flinn

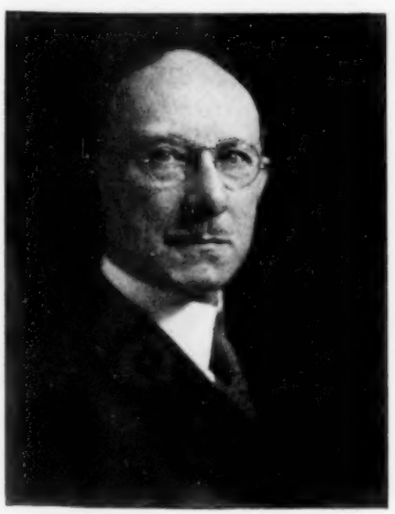
Scarsdale, N. Y., March 14.—Dr. Alfred Douglas Flinn, since 1922 director of the Engineering Foundation, 29 West Thirty-ninth Street, and formerly deputy chief engineer of the Board of Water Supply of the City of New York, died here this morning in the Heathcote Inn of heart disease, after a long illness, at the age of 67. Until he moved to Scarsdale a few months ago, he had lived in the Park Hill section of Yonkers for twenty-five years.

Dr. Flinn was born in New Berlin, Pa. After graduating in 1893 from the Worcester Polytechnic Institute, where he won the Salisbury Scholarship prize, he took a course at the Massachusetts Institute of Technology. The University of Louvain, at the celebration of its 500th anniversary in 1927, made him an honorary Doctor of Applied Science.

From 1895 to 1902 Dr. Flinn was with the Massachusetts Metropolitan Water Works, chiefly occupied with the Wachusett and Sudbury systems of additional supply for more than twenty communities. He was managing editor of the Engineering Record, 1902-04.

Served on Water Supply Board

From 1905 to 1918 he was associated with the Board of Water Supply, holding a succession of important posts, at times that of acting chief engineer. His work included semi-final and final engineering and economic studies for the development of the Esopus and Schoharie watersheds and the construction of large reservoirs, dams and aqueducts, the cost of which, up to 1918 was approximately $140,000,000.



In January, 1918, Dr. Flinn was appointed secretary of the United Engineering Society, which was succeeded by the Engineering Foundation, the research organization of the national engineering societies. At the same time he also took the position of secretary of the United Engineering Trustees, which he held until June, 1934.

Under his guidance the Foundation organized a number of projects such as the Arch Dam Investigation, the Alloys of Iron Research and the Education Research Committee, and other enterprises of a like nature.

In cooperation with the National Research Council, Dr. Flinn aided in developing and financing the Fatigue of Metals Research.

He was assistant secretary of the John Fritz Medal Fund Corporation, 1920–35, and secretary of the Daniel Guggenheim Medal Fund, 1928–36.

Welding

Recognizing the need for a comprehensive national cooperative investigation in the welding field, including a critical digest of the world's welding literature, Dr. Flinn assisted in the formation and the launching of the Welding Research project and was instrumental in securing the necessary initial aid from the Foundation Board.

Member of Many Societies

He was a fellow of the American Association for the Advancement of Science, a member and former director of the American Society of Civil Engineers, former president of the Personnel Research Federation and the Municipal Engineers of the City of New York, and a member of many other scientific groups, also the Century Association, Engineers Club, Sigma Xi and Sigma Alpha Epsilon fraternities.

He was co-author of a "Waterworks Handbook," now in its third edition; originator and for twelve years editor of the "Research Narratives" printed by the Engineering Foundation. For two years he lectured to the senior class of the Lawrence Scientific School and he had delivered the Chester S. Lyman Memorial lectures at the Yale Sheffield Scientific School.

Surviving are his widow, who was Mary Brownell Davis of Boston at their marriage in 1900; a brother, Professor Frederick B. Flinn of Columbia, and a sister, Mrs. Arthur Ramsdall.

Elihu Thomson

Swampscott, Mass., March 13.—Dr. Elihu Thomson, scientist and pioneer in electrical development, died at his home today at the age of 83.

The inventor, originator of electric resistance welding, had been ill since January. A contemporary and friend of Thomas A. Edison, he had only a few years ago cooperated in building the huge seventeen-foot quartz reflector for the world's greatest telescope.

Was a Leading Modern Scientist

The name of Elihu Thomson has been one of the foremost among great men of science of this age. He was one of the pioneers in the field of electrical science, and if he was not associated in the public mind with inventions as dramatic as those of Thomas A. Edison it was because he was always more the scientist than the contriver.

His technical work was directly reflected in practical developments. He was one of the most far-sighted of the early arc-light inventors, and he experimented with the principle of alternating current transmission far in advance of commercial demands. No less than 800 patents were held by Professor Thomson, and he was honored by scientific societies and institutions the world over.

A group of distinguished scientist paid tribute to Dr. Thomson on his eightieth birthday in 1933. They heard Dr. Karl T. Compton, president of the Massachusetts Institute of Technology, characterize him in these words:

"More than any man now living, or, in fact, more than any man in history, Professor Thomson has combined in a most remarkable way the constructive powers of the inventor, the thoroughness and soundness of the man of science and the kindly balance of the ideal philosopher, teacher and friend."

Professor Thomson originated the resistance method of electric welding, which has been in continuous use from 1887 to the present time. He developed the repulsion type of induction electric motor. He invented the magnetic blow-out principle in lightning arresters and electric switches, the oil-cooled type of transformer, the constant-current transformer and the modern process of commercially treating fused quartz.

In 1883 Professor Thomson was the principal organizer of the Thomson-Houston Electric Company, which was merged with the Edison General Electric Company to form the General Electric Company of today. Mr. Thomson had been associated with the General Electric

Company, and at his death he was the dean of the company's staff of scientists. For more than fifteen years he was director of the Thomson Research Laboratory, located at the River Works of the General Electric Company at Lynn, Mass.

Delved in All Electric Fields

In the field of electrical science there was scarcely any aspect in which Professor Thomson had not been active at some time or other. More than ten years before Hertz in Germany discovered the electromagnetic waves of radio, Professor Thomson was demonstrating—in 1875 at Philadelphia—the transmission of signals without wires. He discovered the three-phase electric dynamo machine in 1879 and, during the same year, he anticipated experimentally the modern practice in transformer work. He invented the first practical watt meter, thereby winning half of the grand prize offered at the close of the Electrical Exposition held in Paris in 1889.

Professor Thomson was active outside of electrical matters. He invented the centrifugal cream separator and the centrifuge, an instrument now used universally in biological laboratories. He also devised the fluid pressure engine and the fused quartz mirror for astronomical telescopes.

Since he was a boy he had made a hobby of astronomy. He was also interested in microscopes, autochrome photography and in the building and playing of pipe organs.

Elihu Thomson was born in Manchester, England, the son of Daniel and Mary A. Rhodes Thomson. His father, a Scotsman, was a skilled engineer and mechanic. When the lad was 5 years old the family came to the United States and settled in Philadelphia, where the boy received his early schooling. While yet two years below the age limit at which pupils were admitted to the Central High School, he was prepared for the high school course. During the period of enforced idleness his inherited mechanical instincts led him to build models of pumps, and later into experiments with electricity.

In high school he continued his researches into electricity and chemistry and was soon called upon to assist in the chemical department of the school. From then his promotion was rapid, until, in 1876, when only 23 years old, he was appointed to teach chemistry and physics at the Central High School.

Met Co-Workers of Later Life

It was at this school that Professor Thomson met his co-workers, Professor E. W. Houston and E. W. Rice Jr., whose names were associated with his own in many electrical inventions. In 1880 he resigned the professorship to take the position of electrician with the American Electric Company, which became the Thomson-Houston Company.

In 1876 and 1877 Professor Thomson, in illustrating lectures before the Franklin Institute, Philadelphia, made use of his first practical dynamo.

While professor in the Central High School, Mr. Thomson discovered the principle of electric resistance welding which he developed for commercial use several years later. The American Electric Company, at New Britain, Conn., was organized expressly to market his own original arc light dynamo and electric lighting system. This was the historic dynamo which first introduced the three-phase winding of the internal circuit, and was capable of producing either direct current or three-phase alternating current, according to whether a commutator or collector rings were used with it.

That was the start of Professor Thomson's career as an electrical inventor and scientist to a manufacturing corporation. It was likewise the beginning of one root of the present General Electric Company.

Won Three Most Coveted Prizes

Elihu Thomson was the only scientist in the world who had been awarded all three of the great prizes of the English scientific and engineering world. In 1916 he received the Hughes medal from the Royal Society of London. In 1924 he was awarded the Kelvin medal, one of the most cherished of distinctions, and in 1927 he received the Faraday medal from the institution of Electrical Engineers.

In the United States Professor Thomson was the first man to receive the Edison medal of the American Institute of Electrical Engineers, in 1910. In 1916 he received the John Fritz medal, bestowed only by unanimous recommendation of the four principal American engineering societies. He also received the Elliott Cresson gold medal from the Franklin Institute in 1912. In 1902 he was awarded the Rumford medal from the American Academy of Arts and Sciences. In 1925 he received the John Scott Legacy medal of the Franklin Institute.

For many years he was a member of the corporation of the Massachusetts Institute of Technology and he was acting president of the institute from 1921 to 1924. He was president of the International Electrical Congress held at St. Louis in 1904, and he was one of the six American delegates to the Electrical Congress at Chicago in 1892. He succeeded Lord Kelvin as president of the International Electro-technical Commission and served from 1908 to 1911.

In 1884 he married Miss Mary L. Peck of New Britain, Conn. She died in 1916. In 1923 he married Miss Clarissa Hovey of Boston. There were four sons by the first marriage, Captain Stuart Thomson, who died in 1919 from disabilities resulting from service during the World War; Roland D., Malcolm and Donald T. Thomson.

Honorary Member American Welding Society

Professor Elihu Thomson was an Honorary Member of the AMERICAN WELDING SOCIETY.

THE ENGINEERING FOUNDATION
WELDING RESEARCH COMMITTEE

Sponsored by American Welding Society and American Institute of Electrical Engineers

Issued as a Supplement to the Journal of the American Welding Society, April 1937

PROGRESS REPORT

Engineering Foundation—Welding Research Committee, March 31, 1937

Introductory Remarks

The Welding Research Committee was organized during the latter part of 1935 under the sponsorship of the AMERICAN WELDING SOCIETY and the American Institute of Electrical Engineers following a general conference which had been previously called by Engineering Foundation to explore the need of, and the probable financial support for, a comprehensive, unified research program in the welding field.

Industry was invited to indicate its attitude to the project as comtemplated by the Committee at a luncheon-conference on May 29, 1936 of a few selected leaders, which was arranged by Engineering Foundation and at which Mr. G. L. Knight, President of United Engineering Trustees, Inc., presided. As a result of this conference about $8000 was pledged which, with an additional grant of $5000 from Engineering Foundation, assured the successful initiation of the project, and a central office was established in the Engineering Societies Building.

The late Director of Engineering Foundation, Dr. Alfred D. Flinn, recognized the rapidly increasing importance of welding in industry and the need for research on a coordinated basis during this expansion period. The successful initiation of the project was largely due to his wisdom and vision.

Finances

The contribution pledges made to the Committee to date exclusive of the Engineering Foundation appropriation amounts to $16,600.

Membership

The Committee has been increased since organization and now comprises 12 individuals representing the following industries:

Arc Welding Apparatus and Supplies
Gas Welding Apparatus and Supplies
Pressure Vessel Manufacturers
Machinery
Public Utilities
Railroads
Resistance Welding
Steel
Structural Steel Fabricators
U. S. War Department

It is expected that additional representatives will be secured from a number of other groups including the oil industry, manufacturers of electric welding apparatus, the automotive and aircraft industry and the shipbuilding industry.

In addition to the above members of the Main Committee, there are a total of 130 members on the three committees carrying on the project, namely Fundamental Research Committee, Industrial Research Committee and Literature Committee.

Official Organ

Acting upon the suggestion of Dr. Flinn, arrangements were made with the AMERICAN WELDING SOCIETY for the use of THE WELDING JOURNAL as the official organ of the Committee. Each month there is published a Supplement devoted to the reports and work of the Committee. Equitable arrangements for defraying the cost of the printing have been worked out. Copies of these reports have been widely distributed to cooperators and to research workers in industry, universities and governmental departments.

Fundamental Research Committee

During the calendar year 1936 there appeared ten research reports prepared by the professors engaged in fundamental research in the welding field under this Committee. This phase of the work, which has been carried over from the American Bureau of Welding, now comprises about 50 active research projects in many universities of the country. Although some of these researches are obviously of minor importance, they serve as an important means of acquainting engineering professors and students with the new mechanic art of welding. However, a considerable number of these researches have developed data, methods of analysis and information as to the nature of the phenomena involved, which all told constitute a major contribution to the industries immediately concerned with welding. With adequate financial support it would be possible to establish a number of research fellowships or give grants-in-aid in worthy cases, which would materially expedite progress and increase the value of these investigations.

A list of Fundamental Research Problems in Welding—a Review of Recent Welding Literature, a pamphlet of 40 pages, has been published. It gives to all those interested a list of suggested problems needing solution together with a selected interpretive bibliography of current welding literature on some important phases of welding.

Two methods have been developed to expedite progress and to coordinate activities. One involves personal visitation by members of the Committee staff to the universities where researches are being carried on, and the other is the attendance of the professors engaged in research at a conference held once a year during the annual meeting of the AMERICAN WELDING SOCIETY and the National Metal Congress Exposition.

A unique system of making available translations of important foreign articles to university research workers and also to industrial research workers has been developed by the Fundamental Research Committee through the cooperation of a number of companies and individuals who make the translations and provide mimeographed copies for distribution. The announcement of the availability of these translations is made from time to time in THE WELDING JOURNAL.

Industrial Research Committee

The development of a plan of cooperation and some coordination of the welding research activities in industry is a difficult task. Machinery has been perfected through the establishment of a number of Material Subcommittees, such as, Cast Iron, Carbon Steels, Low-Alloy Steels, High-Alloy Steels, Aluminum Alloys, Copper Alloys, Nickel Alloys; and the establishment of special Functional Subcommittees, such as Methods of Testing, Analysis of Weld Failures and Weld Stresses—Causes and Effects.

A two-day conference was held by the Committee at the Watertown Arsenal during the summer, including presentation of formal papers and plant inspection. Four meetings were held during the year. Another meeting is planned during the annual meeting of the AMERICAN WELDING SOCIETY this fall.

It can be safely stated that probably a large percentage of the outstanding leaders of industrial research in the welding field have been drawn into the picture to serve on one or more of these various subcommittees.

One function of the Industrial Research Committee is to stimulate research through participation in technical programs and publication of papers in technical periodicals. One session at the annual meeting of the AMERICAN WELDING SOCIETY has been set aside for the presentation of such papers.

A consideration of the relatively large scale research projects mentioned below

(*Continued on page 16*)

Welding Cast Steel—A Review of the Literature to November 1, 1936

By W. SPRARAGEN* and G. E. CLAUSSEN†

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SUMMARY

Base Metal.—In good grades of cast steel with carbon content below 0.25% welding procedures are approximately the same as for rolled steel. With carbon content above 0.25% or with special alloy compositions precautions are necessary and sometimes special procedures. Segregations of phosphorus and sulphur should be removed from areas to be welded. The problem of overcoming shrinkage in repair of castings requires special care.

Heat-Treatment.—Strain or stress-relieving for welds in cast steel, especially repair work, is desirable. In higher carbon and alloy steels full annealing may be necessary.

Filler Rods.—For arc welding, a high-grade heavy-coated electrode is generally considered essential. A tough, general-purpose electrode (arc) or filler rod (gas) is used for plain carbon castings, but filler metal of the same composition as the casting is used for alloy cast steel.

Preparation for Welding.—Clean surfaces free from sand inclusions, cracks, porosity and dirt are essential.

Technique.—Multi-layer welding, high currents and peening should be employed for low-carbon castings. Low currents, small diameter electrodes (and in some cases preheating and slow cooling) are necessary for high carbon and special alloy castings. For gas welding, only large sections are preheated, locally or generally, to a bright red.

Metallurgy.—The heat affected zone, due to welding, is not critical in low carbon (less than 0.25% C) castings.

Physical Properties.—The physical properties of welds in cast steel and welds joining cast steel to rolled steel are of the same order as similar welds on rolled steel. They will vary depending upon: (1) the welding process, (2) the composition of the weld, (3) heat-treatment and (4) freedom from defects. Strictly speaking, the physical properties of the weld metal in cast steel welds should be compared with weld metal of similar type in rolled steel, and not with the physical properties of unwelded cast steel or rolled steel.

Tensile Strength.—Arc, atomic hydrogen, flash, gas and thermit butt welds in good grades of cast steel with carbon content below 0.30% are excellent. For the best grades of filler metal in arc and gas welds the strength may surpass that of the cast steel. This is also true of flash welds. Shear strength of arc and gas fillet welds is about the same as of rolled mild steel, and 1000 lb. per linear inch per $^1/_8$ inch of leg of fillet welds is a safe design value. (3000 lb. per inch for $^3/_8$-inch fillet.)

Tensile strength of rolled steel to cast steel welds is intermediate between the tensile strength of the two for the best grades of arc and gas welds.

Ductility of welds is about the same as may be expected from welds made in rolled steel under the same technique and with the same materials.

Tensile properties of higher carbon and alloy cast steels depend on the compositions and physical properties of the materials used, as is true also for rolled steels.

Bend Properties.—Ductility obtained from welds by the free-bend method averages 8% for the bare wire and 30% for good grades of covered electrodes; gas welds are intermediate.

Creep Properties.—Although considerably more data are needed, meagre investigations to date indicate that the creep properties up to 500° C. are not materially different from those of welds in rolled steel.

Fatigue Properties.—The endurance limit for good grade of covered electrode welds is about 25,000 psi, compared with 15,000 psi for bare wire. It should be noted that there are many test results available which indicate an endurance limit for good grade welds of the same order as the cast steel, namely, 26,000 psi. Endurance limits of welds joining cast steel to rolled steel are of the same order. The endurance limit will naturally vary depending upon the presence and extent of defects or stress-raisers in the weld.

Impact.—Notch impact values of welds in cast steel and welds joining cast steel to rolled steel are excellent, and are practically the same as the impact values of unwelded cast steel.

* Secretary, Fundamental Research Committee.
† Research Assistant, Fundamental Research Committee.

This report is a contribution of the Fundamental Research Subcommittee to the work of The Engineering Foundation—Welding Research Committee.

Welding Cast Steel

Base Metal

The welding of cast steel presents approximately the same problems as rolled steel of the same chemical composition. In good grades of cast steel with carbon content below 0.25% welding procedures are approximately the same as for rolled steel. With carbon content above 0.25% or with special alloy compositions precautions are necessary and sometimes special procedures. Segregations of phosphorus and sulphur should be removed from areas to be welded. The problem of overcoming shrinkage in repair of castings requires special care.

From the welding standpoint the composition of cast steel is more critical than of rolled steel. But, as Underwood points out (private communication, Feb. 1937), the reason is connected with defects. The repair of defects usually involves welding in restricted locations so that the weld metal is able to contract in one direction only. Unless precautions are taken shrinkage cracks may occur. Furthermore, defects frequently are not completely chipped out, so that they may propagate during welding. With these two exceptions, and presupposing chemically and physically homogeneous base metal, there is no difference between the welding of cast steel and wrought steel.

The carbon content, as Underwood and Ash[1] have found, should range from 0.18 to not over 0.25% to be readily weldable by conventional methods. The higher the alloy content, the lower should be the carbon. Molybdenum and vanadium are said to raise physical properties without destroying weldability. Pearson[2] also found that special precautions were necessary when C > 0.25 or Mn > 0.75. For high-carbon castings he recommends thin electrodes and the lowest possible amperage to keep down the hardness. An anonymous writer[3] emphatically states that steel castings for welded Diesel engine crankcases should be of the best grade obtainable—preferably electric furnace steel because it has a low content of dissolved gases—and should contain 0.20 C. The composition range of steel castings suitable for welding advocated by Longbottom[4] is 0.17–0.25 C; 0.50–0.80 Mn; 0.25–0.40 Si; 0.06 max. P; 0.06 max. S. Newman and Pascoe[5] favor a steel containing 0.14 C, 0.69 Mn, and some Ni. Quinn[4] states that cast steel with 0.20 to 0.30 C welds quite as easily as rolled structural steel, and that special grade alloy steels with carbon and other elements so proportioned as to be readily welded are now available in the form of small castings (no details).

The importance of correctly proportioning the elements in cast steel to be welded was clearly recognized in 1909 by Diegel,[7] who emphasized the necessity for low silicon. A cast steel with 0.15 Si requires 0.75 Mn to make it weldable. The composition should be: C, 0.2–0.3; Si (as low as possible) not over 0.2%; Mn, 0.7–0.8 (with 0.05 Si, Mn may be 0.6); P, not over 0.03; S, not over 0.04. Treuheit[8] also noted that high-carbon, cast steel was difficult to weld, particularly with a high-carbon filler rod. However, Hoffmann[9] places less restrictions on the analysis: up to 0.35 C, 0.40 Si, 0.85 Mn, 0.03 P, 0.009 S. Johnson[11] states that a cast steel containing 0.45 C, 0.40 Si, 0.50 Mn, 0.06 S, 0.05 P, has good welding qualities. Preheating is imperative for cast steel containing 0.4 to 0.5 C, according to Keel.[10] Cast steel containing up to 2% Cu can be arc and resistance butt-welded, but the copper should not be over 1% for gas welding, according to Zeyen and Mehl,[12] who also discuss the possibilities of precipitation hardening in welds.

Discussing segregated areas in cast steel, which are often the areas that must be dealt with by welding, A. M. Roberts[13] states that segregations of sulphur and phosphorus are more dangerous than carbon in producing cracks. The microscopically thin but extensive cracks often found at the boundary between base metal and weld metal in defective regions near risers are attributed to hot shortness. Analyses of segregated areas showed that 0.65 C, 0.04–0.081 S with 0.0 Mn; 0.48 C; or 0.148 P with 0.40 C increased the tendency to cracking because carbon, sulphur and phosphorus increase the effective shrinkage during cooling and decrease the capacity for deformation. The dangerous range is between the melting point and 650° C. The effect of sulphur is due to Fe S, and can be avoided by using electrodes containing Mn. Electrodes (no details) specially designed to reduce dilatation are said to reduce the effects of carbon and phosphorus.

Fatigued castings should not be welded, according to Baggott[14] and Pearson.[2]

Heat-Treatment

Strain or stress-relieving for welds in cast steel, especially repair work, is desirable. In higher carbon and alloy steels full annealing may be necessary.

Heat-treatment is considered advisable for welds in cast steel.[15,16,17,18,19] Underwood and Ash[1] and Dorrat[20] favor stress annealing, heating slowly and holding 1 hr. per inch of heaviest section. The same procedure (1100 to 1200° F. followed by furnace cooling to below 500° F.) is recommended for repaired castings by Hiemke,[80] who also produces evidence that 4 hr. at 900° F. is reasonably effective for castings that must not be scaled or unduly distorted.

Pearson[2] favors full annealing. All welds in plain carbon cast steel should be either full annealed 870–900° C, or stress annealed 650–680° C. David[21] found that cast steel locomotive frames should be heated to a cherry red after welding, and the Cast Metals Handbook[22] states that heat-treatment is necessary only if a hard zone is produced. Tests on cast steel containing 0.25 C, 0.75 Mn, 0.38 Si, reported by Quinn,[23] showed the average Brinell values in Table 1.

Table 1—Hardness of Cast Steel Welds after Heat-Treatment. Quinn[24]

Method of Welding	Average Brinell Hardness		
	As-Welded	Drawn—1275° F.	Normalized and Drawn
Metal Arc	127	121	118
Oxyacetylene	125	108	102
Atomic Hydrogen	155	137	110

If air hardening is a factor, Jennings[78] recommends annealing at 1200° F. (650° C.) or preheating to 300 or 350° F. (150–180° C.). Local annealing by excessive puddling, excessive currents or reinforcing layers is also said to be effective.

Hoffmann[9] stands alone in asserting that annealing is unnecessary.

It may be mentioned at this point that a number of writers, notably Namack and Hobart,[80] and Mochel,[79] show the application of steel castings to welded steel construction, but in all cases no details of any significance are given.

Filler Rods

For arc welding,[23,24,25] a high grade heavy-coated electrode is generally considered essential. A tough, general-purpose electrode (arc) or filler rod (gas) is used for plain carbon castings, but filler metal of the same composition as the casting is used for alloy cast steel.

According to Pearson,[2] the important qualities of an electrode are: Freedom from porosity, power of penetration, ease of control when meeting obstructions, machinability, mechanical properties and current consumption. If there is any doubt as to quality and design of casting, small diameter electrodes should be used with sufficient amperage only to control slag.

Simonis,[26] and Schimpke and Horn[27] believe that the electrode should have the same tensile strength as casting, whereas Osann[16] and Geiger[17] believe the composition should be the same. For gas welding the rod should have the same composition as casting (low-carbon)[10] in Keel's opinion. The advantage of covered electrodes, according to the Lincoln Electric Co.,[24] is that they do not ground, and therefore permit the bottom of the defect to be penetrated. The Quasi-Arc[28] electrode (1933) for cast steel was the same as for the general welding of mild steel. Treuheit[8] used mild steel electrodes up to 0.78 inch diameter for cast steel, and an anonymous writer[92] recommends the use of Cr-Ni steel electrodes containing 9 and 20% Ni, bare for gas, coated for arc. Hoffmann[9] furnishes Table 2 and Roll[25] recommends Swedish iron rod for gas welding, coated for arc (20–30 volts, 250–300 amps.)

Table 2—Composition of Filler Rods and Electrodes for Cast Steel. Hoffmann[9]

Kind of Welding	C	Si	Mn	P	S
Oxyacetylene	Max. 0.12	0.15	0.4	0.025	0.035 copper coated
Metal Arc	"	0.08–0.10		"	"

A special flux-coated electrode (no details) is mentioned by A. M. Roberts[13] for welding cavities in cast steel. After the walls of the cavities have been lined with the special electrode, a normal electrode is used for filling the remainder.

Preparation for Welding

Clean surfaces free from sand inclusions, cracks, porosity and dirt are essential.

Jennings[30] found that there was little difference in quality of cast steel welds made on as-cast or machined scarves. Sand blasting the surfaces to be welded is recommended by Pennington,[19] Cosgro[31] and Dorrat.[20] The last-named points out that areas to be welded should be tested for soundness, and, if necessary, built up by welding. Surface flaws that may develop in the casting during welding are chipped out and built up by welding. Surface defects and sand inclusions, as Stevens[32] points out, sometimes render steel castings unsuitable for welding. Underwood and Ash[1] recommend that the scarves to be welded be ground, polished and etched to detect minor cracks. Pearson[2] describes the use of the carbon arc to detect whether a crack has been completely chipped or machined out. The arc is passed over the scarves, causing local expansion. Carbon dust settles in the cracks. A. M. Roberts[13] is of the opinion that the process is likely to cause cracks. Defects are chipped to an angle of 75°, according to Pennington,[19] and of 80–90° not 60–70°, according to Pearson.[2] An X joint is best for cracks extending through the entire section. Pearson[2] states that cavities with perpendicular walls entrap slag but A. M. Roberts[13] is of the contrary belief. Baggott[14] notes that chipping is better than machining because cracks in steel castings are often in inaccessible positions. (See section on Repairs for further information on types of joints.)

Technique

Multi-layer welding, high currents, and peening, should be employed for low-carbon castings. Low currents, small diameter electrodes (and in some cases preheating and slow cooling) are necessary for high carbon and special alloy castings. For gas welding,[37,38] only large sections are preheated, locally or generally, to a bright red.

Metal Arc.—By properly timing the successive layers, according to Underwood and Ash,[1] and using high currents, which give a wider heat affected zone and a more gradual hardness gradient, multi-layer welding can be applied even to low-alloy cast steel without preheating. Welding should be done in a flat position with $^3/_{16}$- to $^1/_4$-inch electrodes, the weld metal being deposited as shallow layers rather than as cylindrical beads. Peening is the best means for controlling shrinkage troubles, and should be done with a round-nose tool in an air hammer with light rapid blows. Peening should not be applied to the first layer or two of any weld deposit. Welds in air hardening cast steel should not be peened until a sufficient deposit has been built up to prevent the peening tool from setting up local stress concentrations in the hardened zones and possibly causing cracks.

Other writers disagree with Underwood and Ash on three points. Probably the disagreement is explained by the fact that Underwood and Ash deal with low-carbon castings, whereas the other writers have in mind high-carbon or segregated castings.

P. L. Roberts[33] found that multi-layer welding did not prevent the formation of hard zone in cast steel containing 0.3 to 0.4% C. Second, Pearson[2] considers high currents a primary cause of shrinkage cracks. He also states that welding in easy stages may be necessary and welds may have to be placed where contraction strains are equal and opposite. Cleaning each layer is essential, only one-half of the operator's time being spent in actual application of the electrode. First-class welders for cast steel are said to require two-years of training. In the third place, A. M. Roberts[13] found that weld metal deposited in cavities should not be peened. Schimpke and Horn[27] state that electrodes for cast steel should be of smaller diameter than usual for rolled steel, and the current lower and beads shorter in order to minimize shrinkage strains. Polarity depends on type of electrode. Gerbeaux[34] states that it is sand inclusions that make the welding of cast steel difficult, and Hornor[35] suggests a method for floating out sand inclusions. The spot is heated to the molten state, the arc broken, and the molten metal struck a sharp blow with a ball peen hammer, which throws molten sand and slag out of the weld.

It is the general opinion[12,13,16] that preheating is sometimes necessary, depending on composition and design, and on whether air hardening is a factor. If a furnace or charcoal fire is not available, oil or gas burners may be used, according to Underwood and Ash,[1] who recommend 200–400° F. (100–200° C.) for plain carbon castings under 0.30 C. Higher temperatures are necessary for higher carbon or alloy cast steel. If preheating is not feasible, higher currents should be used. In case preheating is adopted, Roberts[13] states that welding

should be followed by a 12 to 24 hour soak at 550 to 600° C. with furnace cooling to 100° C. Castings requiring extensive repairs may have to be returned to the furnace a number of times.

Successful welding of 0.45% C cast steel annealed at 1650° F. (900° C.) and in the form of long, narrow sections is reported by an anonymous writer.[76] The sections were preheated 45 minutes with a kerosene torch and were welded in rotatable jigs using 1/4-in. shielded arc electrodes and oscillating technique. A number of general articles, such as those by Namack and Hobart,[86] have appeared on the principles of design of parts to be welded from two or more simple steel castings. Jacobs[82] briefly describes the welding of rolled and cast steel parts together (1/2 in. thick, 6 ft. long) in jigs using 1/8 to 3/8 inch heavy coated electrodes. In 1924, according to Pennington,[19] it was not considered good practice to weld cast steel within 6 inches of an old weld.

The soundness of welds in a region of a steam pressure casting (steel) containing hot tears and shrinkage cavities is illustrated radiographically by Hiemke,[80] who states that the Bureau of Engineering (U. S. Navy) permits the removal of defects in steel castings in excess of 25% of wall thickness, provided the weld is subsequently radiographed. Lester[36] found that the chief defects detected in the X-ray examination of cast steel welds are insufficient chipping, oxides and gas bubbles.

Oxyacetylene.—Holler[39] believes that cast steel gears should be preheated either entirely to a red heat or locally in the vicinity of the critical sections of arm and rim. For repairs in cast steel, rolled mild steel patches should be used in conjunction with a flux, in Keel's[40] opinion. Lehmann[41] states that the flame should be neutral and Treuheit[8] found that a slight excess of oxygen may be used if desired, flux being unnecessary. The oxyhydrogen torch is applicable only to material up to 1/4 inch thick.

The left-hand oxyacetylene welding of cast steel elbows and special fittings (0.10 C, 0.93 Mn, 0.36 Si) for steel pipe is described by Greger.[87] The castings are 0.12 to 0.16 in. thick; no flux is necessary. Flattening and hydrostatic tests revealed no weakness in the welds.

Thermit.—The thermit process was used for steel castings in 1900 by Goldschmidt,[42] by Tucker[43] in 1912 for a stern frame, and numerous examples of repair jobs by the process have periodically appeared, among which the examples given by Deppeler[44] are representative. Treuheit[8] (1921) stated that the disadvantages of the process are that the molten metal was mixed with slag and that there was difficulty in securing good union without the preliminary use of a molten cast iron wash. Neither of these objections is valid at the present time. The thermit welding of cast chromium-nickel rail steel (0.49 C, 0.58 Mn, 3.20 Ni, 0.84 Cr, 0.36 Si, 0.034 P, 0.034 S), according to Tinnon,[85] is entirely feasible provided the weld is not rapidly cooled. The Brinell hardness of weld metal (ordinary and special Ni-Cr thermit) and heat-affected zones was 300 to 350 (base metal about 200 Brinell).

Carbon Arc.—The carbon arc process for cast steel requires 60–100 volts, 200–600 amps., according to Geiger,[17] and the weld should be peened red hot to remove slag and blow-holes. Schimpke and Horn[27] state that the carbon arc is often used for filling cavities, filler rods being up to 0.6 inch thick, arc length up to 4 inches at 400 amps. The electrode is positive. Geiger states that preheating is not essential for carbon arc welding cast steel.

Other Processes.—Martin[45] mentions the use of the atomic hydrogen process (150 amps.) to repair imperfections in steel castings. Forge welding of cast steel is done only under exceptional circumstances. The burning-on process may be used for heavy, thick castings. Treuheit,[8] who describes the process, states that the surfaces of the fracture, which are coated with borax, are first washed with cast iron, then cast steel is poured in. The cast iron, which should contain 0.8% P and moderate silicon, "eats" the surface of the fracture. Hughes[46] describes the burning-on of broken wrought-iron wheels using superheated (1625° C.) Tropenas converter steel containing 0.17 C, 0.25 Si. Chancey[47] mentions the bronze-welding of cast steel locomotive frames. According to Becker,[48] if the brazed surface on cast steel is worn away, additional bronze should not be deposited because the grain boundaries are opened up. If the affected surface is not machined, cracks will form.

Metallurgy

The heat affected zone, due to welding, is not critical in low carbon (less than 0.25) castings.

The metallurgical investigations of cast steel welding have been confined mainly to the heat-affected zone. Underwood and Ash[1] found that successive metal-arc deposited layers on a steel containing 0.39 C, 0.63 Mn, 0.22 Si, 2.99 Ni (presumably cast) produced the hardness values shown in Table 3. The hardness of the unwelded steel was 230–260 Brinell. Naturally the practical application of layer drawing requires a closely controlled technique if consistent results are to be obtained. Newman and Pascoe[5] determined the approximate effect, Table 4, of single and double layers of low-carbon welding rod deposited on the surface of a low-carbon nickel steel casting containing 0.97 Mn (170 Brinell unwelded). The oxyacetylene deposit was made in a 90° V. A similar series of tests was made on a casting containing 0.28 C, 0.82 Mn (167 Brinell unwelded) as shown in Table 5.

Table 3—Effect of Multi-Layer Deposits on the Hardness of Cast Steel. Underwood and Ash[1]

First bead	579 Vickers Brinell
Second "	318, but 469 in heat affected zone of second bead
Third "	287 " 384 " " " " " third "
Fourth "	270 to 280 throughout

Table 4—Effect of Welding on the Hardness of Cast Steel. Newman and Pascoe[5]

Type of Welding	Depth of Hardened Zone, Inch	Max. Brinell
Metal, arc, single layer	3/32	201
" arc, double layer	7/32	126
Oxyacetylene single layer	1/2	196

Table 5—Effect of Welding on the Hardness of Cast Steel. Newman and Pascoe[5]

Type of Welding	Depth of Hardened Zone, Inch	Max. Brinell
Metal arc, single layer	1/8	220
Metal arc, double layer	1/4	215
Oxyacetylene single layer	Over 1	250

Bartlett[49] determined that the temperature rise was inconsequential in the seats of cast steel valves 1 1/4 to 12 in. sizes during gas and arc welding, and gas cutting at the flanges. Hoffmann[9] found that the maximum hardness in multi-layer welds in cast steel containing 0.29 C, 0.40 Si, 0.60 Mn, 0.035 P, 0.008 S (132 Brinell annealed) was 150 Brinell.

The U. S. Navy (Hiemke[80]) has investigated shrinkage stresses and distortions in steel castings repaired by welding. Stress-relief at 1100–1200° F. (1 hr. per inch) followed by furnace cooling to below 500° F. reduces shrinkage stresses to less than 5000 psi. Distortion measurements on three repaired steel castings (dimensions not given) showed that distortion caused by welding exceeded 0.020 in. in the vicinity of welded areas, but that stress-relief for 4 hr. at 900° F. caused a maximum distortion (aside from distortion caused by welding alone) of only 0.006 in.

Physical Properties (See Appendix)

The physical properties of welds in cast steel and welds joining cast steel to rolled steel are of the same order as similar welds on rolled steel. They will vary depending upon: (1) the welding process, (2) the composition of the weld, (3) heat-treatment and (4) freedom from defects. Strictly speaking, the physical properties of the weld metal in cast steel welds should be compared with weld metal of similar type in rolled steel, and not with the physical properties of unwelded cast steel or rolled steel.

Tensile Strength.—Arc, atomic hydrogen, flash, gas and thermit butt welds in good grades of cast steel with carbon content below 0.30 are excellent. For the best grades of filler metal in arc and gas welds the strength may surpass that of the cast steel. This is also true of flash welds. Shear strength of arc and gas fillet welds is about the same as of rolled mild steel, and 1000 lb. per linear inch per $^1/_8$ inch of leg of fillet welds is a safe design value. (3000 lb. per inch for $^3/_8$-inch fillet).

Tensile strength of rolled steel to cast steel welds is intermediate between the tensile strength of the two for the best grades of arc and gas welds.

Ductility of welds is about the same as may be expected from welds made in rolled steel under the same technique and with the same materials.

Tensile properties of higher carbon and alloy cast steels depend on the compositions and physical properties of the materials used, as is true also for rolled steels.

Bend Properties.—Ductility obtained from welds by the free-bend method averages 8% for the bare wire and 30% for good grades of covered electrodes; gas welds are intermediate.

Creep Properties.—Although considerably more data are needed, meagre investigations to date indicate that the creep properties up to 500° C. are not materially different from those of welds in rolled steel.

Fatigue Properties.—The endurance limit for good grade of covered electrode welds is about 25,000 psi, compared with 15,000 psi for bare wire. It should be noted that there are many test results available which indicate an endurance limit for good grade welds of the same order as the cast steel, namely, 26,000 psi. Endurance limits of welds joining cast steel to rolled steel are of the same order. The endurance limit will naturally vary depending upon the presence and extent of defects or stress-raisers in the weld.

Impact.—Notch impact values of welds in cast steel and welds joining cast steel to rolled steel are excellent, and are practically the same as the impact values of unwelded cast steel.

Repairs

Examples of repairs are given by a number of writers.[23,50,51,52] Methods are described, in greater or less detail, for the following specific jobs:

Procedure[20] for welding motor spider to cast steel hub.
Arc-welded turbine runner[53] fabricated from steel castings.
Welded spoke[54] in cast steel driving wheel.
Repairs[31] to 4-inch thick cast steel ladles ($^1/_4$-inch electrode, double-V weld with straps).
Thermit welded[55] cast steel stern frame.
Cast steel parts[56] welded to main frame of machine (no details).
Bronze-welded cast steel crusher bowl 6 in. thick, preheated[81].
Arc-welded[57] cast steel stern frame (5 days).
Thermit welded[58] cast steel roll support.
Thermit welded cast steel stern frame.[84]
Metal arc welded[75] cast steel bracket (avoidance of rapid temperature changes and elimination of moisture that had penetrated the casting were the main precautions).

The welding of cast steel locomotive frames by the metal arc process (X weld) is completely described by Vossler[15] and others,[83] and Williams[59] gives similar details for gas welding using a $^1/_4$-inch nickel steel rod. Patch pieces of dead soft iron in conjunction with double V scarves are recommended by Longo[60] for arc welding locomotive frames. About 8 inches on either side of the weld is heated to remove oil and coarse structure. Kohrs[61] describes means for preventing or allowing for distortion in welding fractured locomotive frames. A good description of welding broken cast steel locomotive frames is given by Ziem.[62] The frame is mounted on rollers during welding to permit accommodation to shrinkage. Multi-layer arc welds are used with heavy reinforcement to effect heat-treatment, the reinforcement being machined off subsequently. Artificial prestressing is occasionally used. Frames 50 feet long can be repaired by arc welding with absolutely no distortion. Viall[63] in 1909 reported excellent service from a welded cast steel frame that had been preheated.

The oxyacetylene welding of broken cast steel locomotive frames appears to be practiced extensively in this country and Australia.[77] The fracture is torch cut 90° X. A jack is used to spread the end of the crack $2^1/_2$% of the total length of the crack. The jack is slacked off as required during welding. Two welders are employed, one on each side, but not simultaneously. Welding is started at the top and proceeds downward, using $^1/_4$-in. silicon-manganese rod (no details). The weld is made on one side for several inches and is then peened hot. American practice appears to favor large torch tips delivering 125 to 175 cu. ft. oxygen per hour. Bronze is used for repairing jobs that are not dismantled, in which case less expansion by the jack is required than in fusion welding.

Flame Cutting

The differences between cutting cast and rolled steel are slight and obvious. Everett[64] discusses questions of management in riser cutting in steel foundries, and Kalpers[65] describes the flame cutting of cast steel turbine blades. A method for flame cutting heavy steel ingots is given by *Industrial Gases.*[66] Firth[67] describes the flame cutting of wing-type ingots at 870° C., as stripped from the mold. The best cutting temperature is 790° C. for plain carbon (0.9 to 1.25% C) and alloy steels (1.0 C, 3.5 Cr, also Si-Mn-Mo with a total alloy content of 4.0). Even steel containing 0.30 C, 10 W is cut, the torch-affected surface being scaled off in subsequent rolling. There was no loss from surface cracking of any of the steels.

Suggested Research Problems in Cast Steel Welding

1. Base Metal. Complete investigations of physical properties, welding characteristics, and hardness penetration of low-alloy cast steel welds, particularly of cast steel containing copper.

2. An investigation of welding cast steel (plain carbon) with low-alloy, low-carbon or austenitic filler rods.
3. If weld metal and junction zones are not more coarse grained than heat-treated casting, is heat-treatment above 600° C. (stress-relief) necessary?
4. Shrinkage Stresses in cast steel welds.
5. Low-temperature tensile properties of cast steel welds.
6. Fatigue and vibration tests of welded cast steel structural elements.
7. The thermal expansion of weld metal compared with cast steel of the same composition.

Appendix—Physical Properties of Cast Steel Welds

Practically all values for mechanical properties given in the literature have been summarized in the following tabulation. A separate section is devoted to each property, the investigators being listed chronologically.

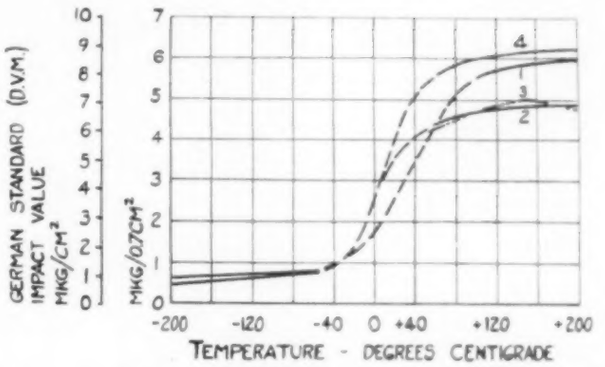

Fig. 1—Notch Impact Value of Arc Welds in Cast Steel. F. Zimmermann[74] (1934).

1. Unwelded Cast Steel
2. Welded, Coated Electrode
3. Welded, Annealed 600°C
4. Welded, Annealed 930°C

Tensile Properties

Investigator		Experimental Details		
		Property	Bare Electrodes	Coated Electrodes
Jennings,[78] 1937	Butt welds in cast steel containing 0.28 C, 0.86 Mn, 0.47 Si	Yield point, psi	40,000	50,000
		Tensile strength, psi	58,000	70,000
		Elongation % in 2 in.	6.0	26.0

Sampson,[52] 1936

Cast steel (0.31–0.39 C, 3 x 12, 1/2 and 1 in. thick, flat cast)
Grit blasted (as-cast) and sawn surfaces gave the same results. Electrodes were mainly 5/16 inch, 20–40 volts, 190–250 amps., 4 to 24 layers

Specimen (Butt-Welded)	Tensile Strength, psi
Cast steel unwelded	64,300
" " to cast steel, bare electrode	61,100 failed in weld
" " " " " " fluxed "	66,900 " " "
(Many specimens did not fail in weld)	
Cast steel to rolled plate (53,300 psi unwelded)	57,640
Flash-welded cast steel to cast steel	all failed in cast steel

The reinforcement was machined from the flash welds but not from the others.

Specimen (Side Fillet Welded)	Shear Strength, Pounds per Linear Inch
Cast steel to cast steel (3/8 x 1 1/2 in. welds)	14,000
" " to plate stock " " " " "	10,000 to 13,000

The grip bars were 1 in. x 3 in. x 12 in.; the straps 3/4 in. x 2 in. x 4 1/2 in.
Inaccurate fitting of the surfaces had an appreciable effect in lowering the strength.

Hiemke,[80] 1936

Metal Arc Welds in Cast Carbon-Molybdenum Steel
(No Details)

Specimen	Proof Stress, psi	Tensile Strength, psi	Elongation % in 2 In.	Reduction of Area, %
All-weld-metal, 0.505 in. diam.	48,000	62,100	38	69.1
Unwelded cast steel, 0.505 in. diam.	50,500	74,300	29.7	51.5
Specimen cut transverse to weld 0.965 x 0.955 in.		63,400	34.0	55.7

Underwood and Ash,[1] 1935

Tensile Specimens V-Butt Welds	Tensile Strength, psi
Full Section	62,000 to 70,000 (Rupture in base metal away from weld)
Reduced Section (Machined to force rupture in center of welded area)	70,000 to 75,000

Becker,[48] 1935

	Tensile Strength, psi
Welded	45,000
Brazed	50,000

White, Corey & Clark,[69] 1934

Welded Seamless Pipe to Cast Steel

	C	Mn	Si	Al	Cr	P	S	Ni	Mo
Pipe	0.33	0.75	0.06	0.04	0.06	0.01	0.02	..	..
Casting	0.24	0.62	0.36	Metallic ..	0.82	..	..	1.19	0.40
Electrode	0.10–0.15	shielded arc.		72° U weld with backing strip					

Pipe-to-casting weld 1 bead 5/32 inch diam.electrode and 11 beads 5/16 inch diam. electrode

Specimen	Tensile Strength, psi 80° F.	Tensile Strength, psi 850° F.	Yield Stress, psi 80° F.	Yield Stress, psi 850° F.	Proportional Limit, psi 80° F.	Proportional Limit, psi 850° F.	Elong. % in 2 In. 80° F.	Elong. % in 2 In. 850° F.	Reduction of Area, % 80° F.	Reduction of Area, % 850° F.	Failure
Pipe	73,067	56,700	40,000	25,080	32,915	13,667	32.2	31.2	60.07	70.1	
Casting	100,850	86,815	70,670	61,750	55,250	37,250	13.3	13.0	23.13	13.06	
Casting-to-pipe	62,650	51,633	41,500	28,000	35,500	16,000	14.5	19.0	32.03	51.7	Junction and weld

Welds were stress-relieved by induction heating to 1100° F. (600° C.) for 1 hr. and cooling in air. All results are averages of three specimens. The welded specimens were standard 0.505 in. diam. machined tensile specimens, 2 in. gage length. Weld metal constituted the middle one-third of the reduced section, and base metal the two outer one-thirds.

Jennings,[20] 1933

	Composition	Tensile Strength	Yield Pt.	Elong. % in 2 In.	Red. of Area %	Endurance Limit psi
Cast steel	0.28 C, 0.86 Mn, 0.47 Si	77,000	42,500	16	32	26,000
Hot-rolled steel	0.12 C, 0.50 Mn, 0.02 S, 0.02 P	55,000	30,000	38	68	27,000

Electrodes 3/16 in. diam. 190 amps. fluxed; composition not stated
" " " " 180 " bare; " " "

Specimens: 60° V butt, 3/4 in. thick, 1 in. wide at reduced section, 2 in. radius, machined. Root of V was chipped out and rewelded.

Specimen	Electrode	Scarf	Tensile Strength, psi	Failure
Cast steel to cast steel	Bare	Cast	68,700	In weld
"	Fluxed	"	74,700	In fusion zone
"	Bare	Machined	61,300	"
"	Fluxed	"	77,500	In weld
Cast steel to hot-rolled steel	Bare	"	61,600	Weld and fusion zone
"	Fluxed	"	68,700	In hot-rolled steel at fusion zone

Specimens: end fillet welds; straps 5 x 2 1/2 x 3/4 in. thick grip bars 16 x 2 1/2 x 1 in. thick 3/8-inch fillet welds

Specimen			Tensile	
Joint Bar	Grip Bar	Electrode	Strength, psi	Failure in:
Cast steel	Cast steel	Bare	66,500	Throat
"	"	Fluxed	80,400	"
Hot-rolled steel	Cast steel	Bare	64,200	"
"	"	Fluxed	78,200	"
Cast steel	Hot-rolled steel	Bare	63,300	"
"	"	Fluxed	72,600	Shear plane
Hot-rolled steel	Hot-rolled steel	Bare	63,400	Throat
		Fluxed	71,800	Shear plane

All results are averages of two specimens. If the tensile strength of the weld metal greatly exceeds that of base metal, failure occurs by shear.

Quinn,[23] 1932

Specimen		Tensile Strength, psi	Yield Point, psi	Elong. % in 2 In.	Red. of Area %
(6) Plain C 0.30–0.35 C cast steel 45° × joint 220 amps. not peened		60,020	39,562	18.5	46.8
(6) ditto 300 amps. reversed polarity		57,604	36,375	19.2	42.3
(2) ditto bare electrode shielding gas contained hydrogen plus 15% propane		84,655	62,467	21.0	40.7
Oxyacetylene. Normalized 1 hr. 1650° F.					
Unwelded drawn 1 hr. 1200° F. 0.28 C, 0.71 Mn, 0.39 Si		74,000	41,000	25.0	40.0
(6) Welded drawn 1 hr. 1200° F. 0.28 C, 0.71 Mn, 0.39 Si		59,850	36,500	14.4	44.1
Oxyacetylene. Normalized 2 hrs. 1650° F.					
(2) Unwelded 0.35 C, 0.69 Mn, 0.35 Si, 0.033 P, 0.040 S		87,850	49,425	N. D.	34.0
(5) Welded 0.35 C, 0.69 Mn, 0.35 Si, 0.033 P, 0.040 S		76,730	46,020	N. D.	25.7
(3) Oxyacetylene. Plain carbon cast steel as-welded		64,166	44,933	N. D.	15.0
(6) ditto 1 hr. 1650° F. air cooled		67,516	44,866	N. D.	27.3
(6) ditto normalized and drawn 1 hr. 1200° F. furnace cooled		59,866	36,575	N. D.	44.1
(2) Unwelded	Oxyacetylene. Normalized and drawn after welding with rods containing 0.40 C, 150 to 1.80 Mn, some Mo	97,500	57,975	N. D.	39.7
(4) Welded		96,962	58,987	N. D.	32.3
(1) Welded	Atomic hydrogen; 0.27 C, 0.73 Mn, 0.42 Si. Rod of same composition. Normalized 40 min. 1650° F., drawn 1 hr., 1275° F.	61,500	38,750	23.7	57.5
(2) Unwelded	Atomic hydrogen; casting; 0.35 C, 0.69 Mn, 0.347 Si, 0.033 P, 0.040 S; Rod, 0.18 C, 1.40 Mn, 0.50 Si	87,850	49,425	25.0	34.0
(8) Welded	"	70,737	46,868	9.6	25.8
(2) Unwelded	Atomic hydrogen; casting, 0.355 C, 1.40 Mn, 0.47 Si; 0.035 P, 0.039 S. Normalized 1650° F, drawn 500° F, 12 hrs.	99,600	60,050	28.0	56.6
(8) Welded	As above. Normalized 1525° F. after welding. Rod: 0.25 C, 1.50 Mn, 0.40 Si, 0.40 Mo	87,768	58,962	10.1	25.1
(4) Welded	As above, but normalized 1525° F., machined, drawn 12 hrs., 500° F.	85,887	54,912	13.3	41.4
(5) Welded	Atomic hydrogen; casting: 0.32 C, 1.25 Mn, 0.32 Si, 0.33 Mo, 0.033 P, 0.0007 S	79,875	45,050	20.0	47.2
(1) Unwelded	Atomic hydrogen; casting: 0.38 C, 1.24 Mn, 0.42 Si, 0.74 Cr, 0.33 Mo. Normalized 1650° F. Drawn 1275° F.	103,000	70,500	21.9	31.1
(2) Welded	Rod 0.44 C, 0.55 Mn, 0.64 Si, 18.22 Cr, 0.91 Cu. Normalized 1650° F. drawn 1275° F.	73,125	63,250	5.5	18.4
(4) Welded	As above. Full annealed 2 hrs., 1650° F.	89,587	53,625	9.2	9.7

(1) Unwelded	Atomic Hydrogen: Casting: 0.30 C, 1.32 Mn, 0.45 Si. Welded with rod of same composition. Normalized 1650° F., drawn 1275° F.	65,500	41,250	17.2–18.5	39.4–51.4
(2) Welded	As above: rod contained 0.47 C, 1.98 Mn, 0.56 S	81,500	48,500	11.7–18.5	25.1–57.5
(4) Welded	Coated electrode: 0.13 C, 0.46 Mn, 0.04 Si. Casting 0.27 C, 0.78 Mn, 0.38 Si	53,375	36,250	18.3–55.1	N. D.
(2) Welded	Normalized 40 min., 1675° F., drawn 1 hr., 1310° F., as above but 40 min., 1675° F., 1 hr., 1650° F. O.Q., drawn 1 hr. 1000° F.	70,760	51,705	13.6–41.4	N. D.

Numbers in parentheses are the number of specimens averaged for each result. N. D. = not determined.

Hoffmann,[9] 1929

% Carbon in Cast Steel	Tensile Strength, psi: Base Metal	Arc Weld	Oxyacetylene Weld	Elong., %: Base Metal	Arc Weld	Gas Weld	Brinell Hardness: Base Metal	Arc Weld	Gas Weld
0.2	62,500	....	....	16	19	21	150	...	160
0.3	72,500	68,500	64,000	12	16	16	175	200	185
0.4	81,000	74,000	78,000	8	12	12	190	225	200
0.5	87,000	81,000	89,500	5	10	10	220	240	220

		Tensile Strength, psi	Yield Point, psi	Elong., % in 2 In.	Red. of Area, %
Candy,[70] 1931	Unwelded cast steel 1 in. diam.	64,560	36,200	31–36	56.9–58.3
	Welded: bare electrode, average of 5 commercial welds.	52,640	30,160	5–10	14.7–21.8

Treuheit,[8] 1921 — Cast steel 0.8 and 1.6 inch thick 0.05–0.15 C, 0.07–0.12 Si, 0.24–0.68 Mn

Type of welds	Tensile Strength % Unwelded	Elongation % $L = 11.3\sqrt{A}$
Forge welds	81.8–106	16
Oxyacetylene	60.7–99	13
Metal arc, 0.16 inch electrode	31.8–72	1
Carbon arc, 1/2 inch filler rod	24.1–60.1	1

The elastic limit was not definite for any of the welds. L = Gage length, A = Cross section

Cross-Bending Properties

	Experimental Details	Modulus of Rupture, psi
Sampson,[68] 1936	See same investigator; section on Tensile Properties	
	Cast steel to cast steel, metal arc weld	124,000 to 133,000
	" " " " " flash weld	126,900

Free-Bend Properties

Jennings,[78] 1937 — Welds in cast steel containing 0.28 C, 0.86 Mn, 0.47 Si

	Bare Electrodes	Coated Electrodes
Elongation % in free bend	10.0	35.0

Sampson[68] 1936 — A. S. M. E. Boiler Code Free-Bend Specimens

Cast steel to cast steel — 63.9 to 85.6% elongation
" " " plate stock, annealed — 50 % "
Both sets of specimens gave 180° bend.

Hiemke,[80] 1936 — Metal arc welds in cast carbon-molybdenum steel (no details). Specimen (1.451 × 1.038 in.) cut transverse to weld gave 75° free bend with 31% elongation.

Underwood & Ash,[1] 1935 — Cast steel; metal arc, multi-layer welds
Free-bend elongation after stress annealing 9 to 24%
" " " after " " 40 to 44%

Jennings,[30] 1933 — See same investigator: section on Tensile Properties.

Specimen	Electrode	Scarf	Elongation %
Cast steel to cast steel	Bare	Cast	8.0
"	Fluxed	Cast	16.0 to 26.7
"	Bare	Machined	8.0 to 10.7
"	Fluxed	Machined	33.3

Specimens were free-bend, face of weld in tension, 10 in. long, 2 1/4 in. wide 3/4 in. thick, 1/64 in. radius fillet on face edges.

Creep Properties

Creep rates in welded specimens of seamless steam-station-piping (0.33 C, 0.75 Mn) to alloy castings (0.24 C, 0.62 Mn, 0.82 Cr, 1.19 Ni, 0.40 Mo) prepared by shielded arc process and stress-relieved by induction heating at 1100° F., air cooled, are reported by White, Corey and Clark,[69] 1934 (see section on Tensile Properites). The rate of creep at 850° F. (455° C.) under a tensile stress of 15,000; expressed in % per 100,000 hrs. and determined by the single step method is:

Pipe material unwelded — 1.1% per 100,000 hours
Welded pipe-to-casting — *1.5% " " "

* Reported as 1.3 in Publication B5, Prime Movers Committee, Edison Electric Institute, 1934.

The test results were not so consistent for the welded specimens as for the unwelded pipe. At a stress of 12,000 psi there was no appreciable creep at 850° F. The duration of the tests was 500 to 600 hours.

Fatigue Properties

Sampson,[69] 1936 — See section on Tensile Properties

For welds in cast steel having all surfaces machined the endurance limit is 20,000 psi (no details).

Jennings,[30] 1933 — See section on Tensile Properties

Specimens were 60° V butt, 1/2 in. diam., machined, stationary cantilever machine.

Specimen	Electrode	Endurance Limit psi
Cast steel (base metal)	...	26,000
Hot-rolled steel (base metal)	...	27,000
Cast steel to cast steel	Bare	15,800
Cast steel to hot-rolled steel	Bare	14,000
Cast steel to hot-rolled steel	Fluxed	23,200
Cast steel to cast steel	Coated	26,000 (ref. 78)

Fatigue curves were horizontal at 2 to 5 × 10^6 cycles.

Sulzer,[71] 1933 — Reversed Bend Fatigue Tests of Welded Cast Steel T Joints. See "Fatigue Strength of Welded Joints," Appendix B, Table 5.

Repeated Impact Properties

Pearson,[72] 1931 — Welded cast steel locomotive smoke box saddle was struck repeated blows by a 3500 lb. tup falling 27 feet. The casting deflected 7 inches and eventually failed at a distance from the weld.

Chapman and Service,[73] 1925 — Six drop test specimens each 4 in. x 4 in. x 4 feet long were welded of cast steel for locomotive frames (80,000 psi tensile strength annealed). The ends were sawn square for thermit welds; 90° V joints were used for metal arc welding. The results of the tup tests were widely scattered, better results being obtained with thermit welds.

Notched Bar Impact Properties

White, Corey & Clark,[60] 1934 — (See section on Tensile Properties for analyses of materials and other details)

Charpy keyhole specimen was used.

	Temperature 80° F.	850° F.
Unwelded pipe	35.3 ft.-lb.	18.3 ft.-lb.
" casting	28.7 "	19.5 "
Welded casting-to-pipe	39.7 "	16.5 "

After heating for 1000 hrs. at 850° F. under a stress of 7000 psi the Charpy values were:

Unwelded pipe	32.3 ft.-lb.
" casting	30.0 "
Welded casting-to-pipe	42.1 "

Temper embrittlement tests revealed no susceptibility.

Zimmermann,[74] 1934 — Results are shown in Fig. 1. Cast steel (0.14 C, 0.60 Mn, 0.38 Si, 0.02 P, 0.02 S) welded (reversed polarity, electrode containing 0.08 C, 0.45 Mn, 0.35 Cu, 0.027 S, 0.027 P, 0.16 inch diam. slagless coating, 170 amps.) with 60° X, 0.12 inch root spacing, 0.6 to 0.8 inch plates. The specimens were 10 x 10 x 55 mm. with 3 mm. notch 2 mm. diam. (0.39 x 0.39 x 2.18 in. with 0.12 in. notch, 0.08 in. diam). Values at −40 to +100° C. are averages of 4 specimens, at other temperatures averages of 3. Only good specimens (radiographic test) were tested. The diffraction pattern of the original material revealed considerable internal stress which was completely removed by annealing at 930° C., which also produced a fine grain structure.

Tensile Impact Properties

Underwood & Ash,[1] 1935 — Tensile impact tests on cast steel multi-layer metal-arc butt welds at 21.35 ft./sec., gave 1668 to 2141 ft.-lb., fracture occurring at defects in weld or casting. The welds were made with commercial mild steel electrodes on cast steel containing approximately 0.20 C, 0.60 Mn, 0.20 Si, and less than 0.05 P and S. The dimensions of the tensile impact specimens were 0.5 x 0.6 x 4.5 in.

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Progress in Welding During the Last Six Months of 1936

(A critical review of recent welding literature by Wilhelm Lohmann, *Stahl und Eisen*, 57, No. 8, pp. 209–214, Feb. 25, 1937. Translated by G. E. Claussen, Assistant to Fundamental Research Committee.)

1. Effect of Materials

Up to the present time the attempt has always been made by welding engineers to produce a weld having as closely as possible the same chemical composition and mechanical properties as base metal, especially in boiler and pressure vessel welding. Fried. Krupp A. G. have adopted quite different tactics by welding ferritic steels with an austenitic rod (usually 25% Cr, 20% Ni). The results of this development are reported by E. Beckmann[1] and K. Kautz.[2] Offhand it would be expected that, by using an austenitic rod with ferritic base metal, a martensitic transition zone would be produced on account of dilution of the austenitic weld metal by ferritic base metal. Actually such a zone is found but the notch impact value of the martensitic zone is higher than that of the base metal. This is ascribed to the low melting point of the chromium-nickel steels and to the accompanying decreased possibility for intermixing and diffusion. It was also thought that difficulty with shrinkage stresses and cracking would be experienced on account of the difference between the thermal coefficient of expansion of weld metal and base metal. However, the weld metal that is used has about the same coefficient of expansion as mild steel. Besides, the weld metal is generally under tensile stress which is decreased on heating because the coefficient of expansion of the weld metal is higher than that of the base metal. The question of annealing is complicated by the fact that low temperature annealing has no effect on the transition zone, whereas high temperature annealing causes undesirable carbide precipitation in the weld. Kautz showed, however, by thorough tests that satisfactory properties were obtained even in the unannealed condition so that the transition zone did not exert a harmful effect on the joint. The question of plastic deformation or cold work is secondary if the weld is made in non-ageing steel. The authors are convinced that there need be no hesitation in using austenitic welds in mild steel even in the unannealed condition.

Supplementing the above are the investigations of C. Carius[3] on the corrosion resistance of austenitic welds in ferritic base metal. Artificial seawater and boiler water were the corroding media. The tests showed no pronounced local action between the austenitic weld metal and the ferritic base metal and that accelerated corrosion need not be expected.

A retrospective view of the development of filler rods shows, as A. B. Kinzel[4] points out, that we have abandoned the old rules that the welding rod should have low carbon and as few alloying elements as possible and that welding rod and base metal should have as nearly as possible the same composition. Especially for high-tensile steels, gas rods and electrodes are now alloyed. A welding rod with 0.5% Si and 1% Mn is highly recommended. Silicon has the property of increasing the solubility of the metal for gases evolved during welding and of preventing excessive oxidation of the deoxidizer. In order to slag off the silica, the manganese must be high. The results of experiments on the substitution of nickel and vanadium for silicon and manganese were not encouraging. By adding sufficient manganese, usually in the coating, porosity, which is especially pronounced in metal arc welding, is not observed in weld metal containing up to 0.2% Si (tests by the reviewer showed that the limit is as high as 0.5% Si). Carbide-forming elements are usually employed for high-tensile welds.

The change of chemical composition of welding rods during welding has been investigated by K. L. Zeyen.[5]

The filler rod used in the tests contained 0.13% C and 0.66% Mn. For gas welding the rod was used without coating. For metal arc welding D.C. and A.C., the electrode was used without coating, and with light, medium and heavy coating. The mechanical properties and structure of welded specimens were compared with the properties of unwelded steel of the same chemical composition. With increasing thickness of coating there was a decrease in loss of carbon, manganese and silicon, as well as a decrease in nitrogen content. Welds made with bare electrodes or with electrodes having a coating of small or average thickness had a high nitrogen content and high strength but low ductility. Heat-treatment (normalizing or stress annealing at 600° C.) did not improve the mechanical properties of welds containing high nitrogen; in several instances the properties were actually impaired. Heat-treatment improved the ductility and toughness of low-nitrogen welds. It is remarkable that, in general, stress annealing for 2 hrs. at 600° C. gave better physical properties than normalizing, which may be explained perhaps by the fact that the coarse grained reinforcement had been machined from the specimens. No difference was found between welds made by D.C. and A.C. The change of composition of rods during surfacing was also investigated. It was found that the loss of carbon and manganese during welding was very slightly affected by coating the electrode but that the percentage lost was smaller as the carbon and manganese content of the electrode was increased, The loss of all elements was decreased by alloying the electrode with chromium and tungsten. No difference was found between A.C. and D.C. when coated electrodes were used. Welding in several layers tended to preserve the alloy content. With the exception of austenitic manganese steel, there was less loss of alloying elements in gas welding than in arc welding.

Hitherto the limit of weldability of steels was considered to be 0.3 to 0.4% C. Zeyen[6] has been able, by improving the filler rod, to weld higher carbon steels without difficulty. Experiments were made on butt- and fillet-welded joints in base metal containing 0.11 to 0.68% C in thicknesses of 0.24 and 0.47 inch. Butt welds were made with a heavy coated electrode of the type: E 52H (according to DIN 1913 electrodes of type E 52H have a minimum static tensile strength of 73,000 psi and contain 0.20% C max., 0.03% P max. and 0.03% S max.; malleability is required). With this electrode all steels could be welded without defects, and up to 0.56% C the welds had bend angles and notch impact values sufficient to meet the specifications for welded low-alloy structural steel having a static tensile strength of 73,000 psi (St 52). Above 0.56% C the bend angle decreased rapidly. The static tensile strength was greatly affected by carbon content; it increased as the carbon content was raised to 0.56%, but decreased with further addition of carbon. Fatigue tests showed that the endurance limit of welded high-carbon steel was the same as that of welded soft structural steel. Greater advantages are gained by using austenitic electrodes, whose high work hardening capacity endows welds in high carbon steels with good ductility and toughness. In agreement with field experience more difficulty was found in welding fillet joints. Electrodes of the E 52H type were satisfactory for steels containing up to 0.4% C. With higher carbon thermal cracks were observed, and lightly coated alloy electrodes or cored alloy electrodes were more suitable for both fillet and butt welds. A filler rod containing chromium and molybdenum was satisfactory for oxyacetylene welding high-carbon steels. In agreement with results of other investigators the alternating bend fatigue strength of oxyacetylene welded joints was considerably higher than that of metal arc-welded joints, which is explained mainly by the more favorable geometrical shape of the gas welds. The author stresses the fact that the results apply only to steels 0.24 and 0.47 inch thick. Greater difficulty was encountered with material $^1/_8$ inch thick. It is the reviewer's belief that greater difficulty would also be experienced if the material was thicker than $^1/_2$ inch. Furthermore, Zeyen showed that weldability may be increased if a suitable alloying element is substituted for part of the carbon.

J. B. Johnson[7] discusses the welding of aircraft steels in America. The methods generally used in America are gas welding with acetylene and hydrogen, D.C. arc welding, spot and seam welding and atomic hydrogen welding. The oxyhydrogen flame, on account of its low temperature, is particularly adapted to welding thin material having a low melting point. The D.C. arc can be used on material 0.05 inch thick and over. Despite the non-uniform bead deposited by the D.C. arc, it is often preferred on account of its small heat effect. A machine with easily controlled current is, of course, essential. The number of materials used in America is still very large. Soft steel rod is used for plain carbon steels; alloy steel rod is used for moderately alloyed steels. The author shows that inclined or fish-mouth joints may be used to raise the strength, in which case static tensile specimens generally fracture in the unwelded part of the tube. Johnson gives the following rules for welding. Welding must never be started at an edge; the weld should be started in the middle and carried toward the edges. Welding should be at a sufficiently slow speed that all defects of penetration can be avoided. Joints between thick and thin cross sections should be avoided. If such a joint must be made the larger cross section should be preheated. Distorted parts should not be straightened by heat. Steels with a tensile strength of over 175,000 psi should be preheated before welding. Soldered joints should not be made near welds. More than six connections in a single cluster should not be made. In special cases it is advisable to put the parts to be welded in compression before starting the weld. The author suggests several types of specimens for qualifying welders. Magnetic methods are best for determining the quality of the welded joint.

A discussion of the requirements for aircraft steels is given by R. H. Dobson and R. F. Taylor[8] in their paper on the present status of aircraft welding. They recommend that the carbon content of chromium-molybdenum steels be kept as low as possible consistent with strength because these steels have a tendency to harden and can give difficulty. Permissible limits of impurities, particularly sulphur and phosphorus, are not given. Likewise, no mention is made of differences in behavior between open-hearth and electric steels. The authors maintain that chromium-molybdenum steels have better mechanical properties and weldability than manganese steels. Welding rod of the following composition is recommended: 0.10% C, 0.60% Mn, 0.20% Ni, 0.05% P, 0.05% S. Emphasis is placed on freedom from slag, rust, etc. Dobson and Taylor believe that, outside of gas welding, the most promising methods for aircraft welding are spot welding and welding in an atmosphere of hydrogen. Arc welding is of no value. Details on this question as well as on the welding of light metals are given in the original article.

A long report on sensitivity of steels in welding is given by K. L. Zeyen,[9] who deals particularly with different kinds of weldability tests for alloy steels. It was found that the customary method of determining sensitivity to welding cracks by measuring the length of the cracks is not satisfactory because only cracks through the entire

not satisfactory because only cracks through the entire thickness are determined. However, surface cracks are equally dangerous, and since they are not usually measured, the usual tests are inadequate. The effect of alloying elements on sensitivity to welding cracks is thoroughly investigated. First of all, increasing the carbon content increases the sensitivity to cracking. In recent years this difficulty has been overcome by substituting manganese, chromium or molybdenum for the carbon in order to maintain high strength. In aircraft construction the composition of the popular chromium-molybdenum steel must be closely controlled in order to avoid welding cracks, since experiments by J. Müller[10] showed that excessive carbon, especially in conjunction with sulphur and phosphorus, has an adverse effect on sensitivity to welding cracks. Opinion is still divided on the actual significance of sulphur and phosphorus content. In addition, the effect of process of steel melting and refining can be important for the sensitivity of chromium-molybdenum steels to welding cracks. The usual belief up to the present time has been that open-hearth steels have a greater tendency to welding cracks than electric steel. For this reason all steels for aircraft construction in Germany are made by the electric process. Zeyen reports, however, that with appropriate refining procedure open-hearth steels can be produced with high-carbon content yet low sensitivity to welding cracks. These steels, however, have a high-manganese content and cannot, therefore, be compared directly with ordinary open-hearth steels. The same results have been obtained by other investigators. Details of the special refining procedure are not given. Zeyen found that electric steels were also affected by method of refining, but here again steels with different carbon contents are compared. Another factor increasing the sensitivity to welding cracks is internal stress caused by heat-treatment which, if we are to accept J. B. Johnson's[11] statement, is removed by normalizing. Whether this treatment is successful is very doubtful, in the reviewer's opinion.

The question of the sensitivity of thin steel to welding cracks, with special reference to aircraft construction, is discussed by F. Bollenrath and H. Cornelius.[12] Welding cracks are caused by stresses of three kinds: internal stresses developed by mechanical and heat-treatment of the steel prior to welding; stresses in the weld and its immediate vicinity caused by welding; and stresses in the structure arising from inexpert sequence of welding joints. The opinions advanced on all sides concerning the dangerous effect of high sulphur alone or high sulphur and phosphorus on the occurrence of cracks are regarded with scepticism by the authors in view of the absence in other countries of strict regulations concerning sulphur and phosphorus content. It is their opinion that the charge is of greater importance for the behavior of the steel in welding, although there is no authoritative experimental information of this question as yet. That there is something in their belief is shown by the fact that steel from the open-hearth furnace is not so good for welding as electric furnace steel. Although the cracks occur at temperatures between 1000 and 800° C., it is not a question of rod shortness, because the manganese content is sufficiently high. The filler rod does not play an important rôle in this connection. Bollenrath and Cornelius suggest special welding procedures to avoid welding cracks; among others, weld in short sections, or gently heat the work in the vicinity of the weld to decrease the quench effect and accompanying hardness. Clamping the work before welding may also give some improvement. The metallurgical problems of aircraft have been discussed elsewhere.[9]

2. Welding Methods

The success of arc welding in the last few years has given new impetus to those striving to reduce the cost of gas welding. For example, C. F. Keel[13] has proposed a process named "activated welding," which is based on the fact that with the usual neutral adjustment a reducing atmosphere is obtained. The heat of formation of acetylene and the heat of combustion of CO is set free during welding in the part of the flame useful for welding. Still the heat of combustion is not utilized. As O. Mies[14] has shown, with excess oxygen, oxidation of iron is avoided because the affinity of oxygen for hydrogen and CO is greater than for iron. However, in the reviewer's opinion, an equilibrium condition will be established in which the iron also will be oxidized, especially since CO_2 at the elevated temperatures of the welding flame has an oxidizing effect on iron. Keel states that there are a number of elements which have a greater affinity for oxygen than iron, for example silicon, manganese, aluminum, magnesium, etc., as in the Bessemer process. By adding such elements to the welding rod an excess oxygen flame may be employed without oxidizing the iron. To verify this, a series of welding tests was made with a filler rod containing 0.19% C, 0.34% Si, 0.86% Mn. One set of specimens was welded with the usual flame adjustment; another set was welded with an oxidizing flame having an oxygen-acetylene ratio of 1.3 to 1.4. It was found that the welding time was decreased 20% by use of the oxidizing flame, and the consumption of acetylene was reduced 15 to 20% for the same oxygen consumption. Tests of mechanical properties revealed no difference between the two methods. The process is applicable to all kinds of welding in steel. An experimental drum welded with the oxidizing flame, when tested to destruction by hydraulic pressure, developed a crack at some distance from the weld.

Other tests to improve gas welding have been made by A. Matting and H. Otte.[15] In their process the weld is made simultaneously from both sides in sections 1.2 to 2.0 inches long. After being welded each section is peened. Then the preceding section is reheated and heavily hammered. The process is called "high-grade gas welding" and is intended to eliminate the necessity for subsequent annealing. Comparative tests were made with unhammered, normalized gas welds and with specimens welded with coated electrodes. These tests showed a definite superiority of the "high-grade" gas welds in strength, elongation, notch impact value and fatigue value. However, some of the curves seem to show that the quality of the weld decreases considerably with increase in plate thickness. On account of frequent interruption of welding and reheating, the new process naturally results in slow welding speeds compared with other methods, especially with welding by means of coated electrodes and with unhammered gas welding. Whether the increased cost is justified depends on the necessary quality of the weld, since a nearly equivalent arc-welded joint can be made at lower cost with uniformly good properties in all commercial thicknesses.

Similar experiments have been made by H. H. Grix.[16] The necessity for beveling the edges in arc welding can be eliminated in gas welding plates up to $^1/_2$ inch thick, if a sufficiently wide gap is left between the plates and two welders work simultaneously from both sides, one welder keeping 0.24 to 0.32 inch in advance of the other. This method, which requires the use of smaller tips, requires the least amount of filler rod and at the same time the smallest amount of oxygen and acetylene. It has the advantage that the leading weld is annealed by the lagging bead. According to Grix, a speed of 39

inches of weld in 15 minutes can be made in plates 0.39 to 0.51 inch thick; that is a total welding time of 30 minutes because two welders are required. For the same welds made by the metal arc process 75 minutes is required. This proves that in many cases gas welding is more economical than arc welding.

Tests of a similar nature on the economy of gas welding in comparison with arc welding have been made by R. Meslier.[17] The tests were made on plates 0.16 to 1.18 inch thick. Right-hand welding, in various modifications, was used exclusively. In plates 0.16 to 0.20 inch thick welds were made from both sides. In plates 0.24 to 0.47 inch thick welds were made from one side as well as from both sides. Only welds made from both sides were executed in plates thicker than 0.47 inch. Arc welds (60°) were made with coated electrodes 0.12 to 0.20 inch diameter, according to plate thickness. With plates 0.79 to 1.18 inch thick, electrodes 0.24 to 0.32 inch diameter may be used and would give more satisfactory results. In the gas welding of thin plates best results were obtained by welding from both sides without beveling the edges. With medium and thick plates, welding from both sides with beveled scarves is preferable to welding from one side. Gas welding costs 15 to 30% less than arc welding. With left-hand welding the results are less favorable because a greater quantity of heat is required, and gas consumption is increased.

Multi-layer welding has hitherto been confined to arc welding and has been responsible to quite an extent for the good ductility of arc welds. Recently the method has been applied to gas welding. R. M. Rooke and F. C. Saacke[18] have prepared a paper on the method of multi-layer gas welding pipe joints and their properties. The angle of bevel was 60 to 74°. The pipes are first tacked at several places. The first layer is begun between two tacks. The bead should not be convex, otherwise defective penetration may be obtained and the gas consumption is increased. It is a matter of indifference whether the pipe is turned or whether welding is done with the pipe in place. The method is especially advantageous for thick pipe over 0.32 inch. The diameter of the filler rod is 0.24 inch for the first bead and 0.32 inch for subsequent runs. The top run is made by directing the flame on the filler rod and is made as rapidly as possible. With a little practice any welder is in a position to use this method for welding thick plate.

Surprising results were obtained in welding 3.5% Ni steel plate $^1/_2$ and $^7/_8$ inch thick in a single layer and also with the multi-layer technique. Bend elongations of 10 to 15% were measured on single-layer welds whereas three-layer welds gave 45 to 50% elongation. Similar results were obtained in the notch impact test. Single-layer welds had a notch impact value of 34.5% of base metal, two-layer welds 48.4%, three-layer welds 66.8%. The multi-layer process is economical particularly for thick material. Table 1 shows that for $^1/_2$-inch plate two-layer welding is best from the standpoints of time and gas consumption. With $^7/_8$-inch plate, however, three-layer welds are more economical than two-layer. All in all, the multi-layer method is advantageous from the standpoint of quality as well as of economy for the welding of joints that cannot be annealed.

The practical aspects of the multi-layer process in the construction of steam lines are discussed by A. N. Kugler.[19] One pipe was $13^3/_4$ inches diameter to operate at 625 psi and 400° C.; the other pipe was $8^1/_2$ inches diameter to operate at 270 psi and 280° C. The wall thicknesses were $^9/_{16}$ and $^1/_2$ inch, respectively. Tests showed the same superiority of multi-layer welding as was determined by Rooke and Saacke.[18] The time of field welding was reduced 15 to 25% by multi-layer welding. The consumption of oxygen and acetylene was reduced to about the same extent. Kugler found that two-layer welding was best for thicknesses of $^3/_8$ to $^5/_8$ inch, three-layer welding for $^5/_8$ to $^7/_8$ inch, and four-layer welding for $^7/_8$ to about $1^3/_{16}$ inch.

Table 1—Welding Time and Consumption of Materials in Single and Multi-Layer Welding Plates and Pipes of 3.5% Nickel Steel of Different Thicknesses (according to R. M. Rooke and F. C. Saacke[18]).

Wall Thickness, Inch	$^7/_8$	$^7/_8$	$^3/_4$	$^1/_2$	$^1/_2$	$^1/_2$	$^5/_{16}$
Number of Layers	3	2	3	3	2	1	1
Time of Welding, Minutes	63	123	60	54	37	69	14.5
Rod Consumption, Lbs.	6.1	5.8	4.9	2.1	1.9	2.2	0.9
Gas Consumption, Cu. Ft. Acetylene	101	152	97	34	34	45	7.8

A new method for building up surfaces by welding is described by M. C. Smith[20] that is based on an earlier development by N. W. Cole. The process makes use of metallic crystals of boron which are resistant to oxidation and attack by acids, and are as hard as diamonds. However, these crystals have a high melting point and cannot be melted in the arc. Further tests showed that these high melting crystals can be produced in a mixture of boron crystals of lower melting point so that welding is made possible. The melting point of the mixture is below 1250° C. It is not desirable to prolong the heating of the base metal during deposition. The mixture of crystals should be merely a very thin superficial layer. This layer cannot be machined. The mixture should not be applied to very soft steels, otherwise pressure will force the hard layer into the soft base metal. The use of steels containing considerable carbon is therefore recommended. No details are given of the composition of the mixture of crystals.

3. Methods of Testing

The X-ray inspection of welds in plates of the same thickness no longer presents difficulty and is sensitive to defects. But fillet welds are more difficult. E. Brandenberger[21] has investigated the possibility of detecting defects in T joints by means of X-rays. The X-rays are directed perpendicular to the leg of the T and the film is placed partly on the leg of the T, partly on the flange. The part of the beam falling on the leg film (perpendicular to the axis of the beam) detects the usual defects. On the other hand, defective penetration at the root of the weld is shown in the flange film in the form of light streaks which radiate perpendicular to the axis of the weld. These streaks definitely locate the defects. This method for X-ray inspection may also be applied with intensifying screens or equalizing wedges as shown by the author's experiments.

The tensile impact test has often been proposed for welded joints as a means of obtaining rather different information about the toughness of welds than is afforded by the notch impact test. This is partly confirmed by tests of O. H. Henry[22] on various welds at temperatures of 20 to −60° C. The following materials were used: a steel containing 0.25 Cu, 0.57% Mn resistance butt-welded, bronze-welded and atomic-hydrogen welded, and a stainless chromium-nickel steel, gas and arc welded. All specimens of the plain carbon steel showed a more or less pronounced decrease of energy absorbed as the temperature was lowered. In every case the decrease was greater in the welded joints than in unwelded base metal. A steep fall in impact energy such as that observed in the notch impact test at low temperatures was not found. Compared with unwelded base metal the different joints rank in the following order

of merit: resistance butt-welded was best, then atomic-hydrogen welded and bronze-welded with high-strength and special brasses. The elongation and reduction of area varied according to the same order, with the exception that atomic-hydrogen welds had the highest ductility. As might have been expected, elongation and reduction of area decreased at lower temperatures. The results with the austenitic chromium-nickel steel were fundamentally different. The tensile impact value increased as the temperature was lowered, but elongation and reduction of area decreased rapidly, especially in the welded joints. The tests confirm the fact that *there is no relation between notch impact value and tensile impact value.* In plain carbon steels the decrease of tensile impact value is associated with a rapid decrease in capacity for deformation. In contrast with the results of the two steels mentioned above, the tensile impact value of an unwelded plain carbon steel with 0.9% C first increased as the temperature was lowered, then rapidly decreased between −20 and −40° C. This difference in behavior between different steels shows that the interpretation of the results of the tensile impact test is even more difficult than of the notch impact test. The velocity of the striking hammer and the velocity of deformation play an important part. The reviewer notes that in several tests at room temperature with welds in the same low-carbon steel, some of the specimens broke at low energies without elongation, whereas others broke at high impact energies with high elongation. It may be inferred, therefore, that small defects in the welds have a large effect on tensile impact results.

4. Properties of Welds

The behavior of joints in high-tensile steels is affected by the properties of the filler rod and by the condition of the transition zone. These, in turn, depend on the heat input and rate of cooling whose action may be dangerous in the presence of alloying elements. W. L. Warner[23] deals with these matters. Steels having a carbon content of 0.12 to 0.35% were used; the alloying elements, which were used singly or in combination, were nickel, molybdenum, chromium, manganese, silicon and copper. The steels comprised most of the important types of the well known low- and medium-alloy structural steels. Three types of coated electrodes containing molybdenum and nickel were used. The electrodes had nearly the same strength but differed in elongation and tensile impact value. The coatings of two of the electrodes were of the mineral type; the third electrode was coated with mineral and organic materials. Independent of the type of electrode, it was found that the depth of the transition zone decreased as the welding speed increased but the difference in hardness compared with unaffected base metal increased decidedly. Furthermore the depth of the heat-affected zone was not changed by varying the carbon content. But the hardness of the heat-affected zone depended primarily on carbon content. The alloying elements had a hardening effect only in conjunction with carbon. With low-carbon contents, even steels with more than 1.5% Ni and 0.9% Cu gave an increase in hardness of only 20%. As a rule, the hardness measurements showed that the effect of carbon in increasing the hardness was augmented by the alloying elements in the following decreasing order: maximum hardening with nickel, then manganese, molybdenum, copper and least silicon. On the basis of a large number of hardness measurements Warner prepared hardness diagrams showing the hardness of the transition zone as a function of carbon and alloy content, and speed of welding.

The properties of the heat-affected zone were determined in additional tests. As is well known, the tensile strength, yield point and tensile impact value of the heat-affected zone was greater than that of base metal, but the elongation was lower. Interesting tests were made on the tensile impact value at low temperatures of nickel steels of the type used in oil refineries. For temperatures as low as −80° C. a steel with 0.2% C and 3.5% Ni gave the best results, but such steel is difficult to weld. The author lists steels in order of increasing strength and hardenability. Of general significance is his statement that the velocity of impact has an effect on the tensile impact value of welded and unwelded specimens.

An investigation of the effect of surfacing by welding on the fatigue strength of steel shafts has been made by G. Kühnelt,[24] who deals with shafts whose original dimensions, decreased by wear, had been restored by surface welding. Service tests showed that such shafts had short life, although prior to surfacing they may have withstood several millions of load reversals without indication of failure. The poor results after surfacing doubtless are to be ascribed to the effects of welding. The investigation included the determination of the effect of the structure at various zones (weld, transition and base metal) on fatigue strength, as well as of the fatigue strength and internal stresses in the welded shafts. One coated and three bare electrodes were used. Specimens from the transition zone had a higher fatigue strength than base metal or weld. Soft steel electrodes gave more uniform results than high-carbon steel electrodes the results from which were widely scattered. Singularly enough the difference in internal stress between center and surface of shaft was greatest with coated and bare soft steel electrodes. By using special welding procedure, considerable improvement in the fatigue strength of experimental shafts could be achieved, but such conditions could not be applied to the welding of thick shafts. Merely by depositing the metal in two layers an increase in fatigue value of about 30% was observed, which was explained by the annealing action of the second layer. The greatest decrease in fatigue strength was observed in materials with the highest strength. Tests on bars with different amounts of weld metal deposited on the surface showed that the fatigue limit decreased as the amount of deposited metal on the circumference of the specimen was increased. In specimens with surface welding over the entire circumference the fatigue value was best in those specimens with most favorable structure in the heat-affected zone. Summarizing, the building up of a worn highly stressed shaft by surface welding should be avoided wherever possible, especially if the shaft is of high-tensile steel. With thin shafts under low stress, or for shafts in which stress raisers, such as fillets and grooves, are present, building up by surface welding is quite satisfactory since the notch effect due to porosity in the weld is then secondary.

The use of low-alloy steels in boiler construction is discussed by L. J. Larson.[25] The determining factor in the choice of materials is not so much the price of the base metal as the ease of fabrication and conditions of service. He cites several examples from the petroleum industry in which chromium and chromium-vanadium steels for high temperatures and nickel steels for low temperatures are more economical than plain-carbon steels. The alloy steels, however, often require special attention in welding. This is particularly true of chromium steels, which tend to air harden in the carbon ranges commercially used. If shrinkage stresses can be controlled successfully, other difficulties need not be anticipated because pressure vessels are stress annealed. Tests

show that the alloy steel drums have strength comparable to plain-carbon steel drums at ordinary temperature, whereas at high temperatures the alloy vessels are superior in every respect. Chromium steels are not suitable for service at low temperatures, for which nickel steels containing 1 to 3.5% Ni and less than 0.3% C are recommended. Besides these, there are a large number of similar steels suitable for different applications. The rule with all these steels is to keep the carbon content as low as possible in order to avoid hardening during welding. The strength of the steel is attained by means of alloying elements.

Tests to determine the best type of beam-column connection are described by I. Lyse and G. J. Gibson[26] who tested angle and plate connections with fillet welds in tension, and beams with column connections in bending. The load causing the first indication of inelastic action as well as the maximum load were determined. Tension tests showed that the rigidity and maximum load increased as the length of the perpendicular leg of the angle was decreased. The same effect was observed with respect to the thickness of the angle. If the weld was made on the thickness of the angle considerable plastic deformation was observed in the angle before the maximum load was attained. The deformation was more extensive as the length of the leg of the angle was increased. Side welds in combination with the fillet welds were not advantageous on account of stress concentrations at their ends. Judging from their experimental results the authors do not recommend the substitution of plates for angles, whether the plates are butt- or fillet-welded. In beam connections with angles attached to the upper flange, permanent deformation occurs under relatively low load. The ultimate strength as well as the maximum deformation were, however, as large as in the tension tests. It was found that the design strength of beam-column connections computed by ordinary methods was considerably lower than the strength developed by the connections in actual tests. The use of angles welded to the upper flange results in a flexible connection, the beam itself thereby being relieved of load

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(*Continued from page 1*)

will clearly indicate the difficulty of sharply separating the work of the Industrial Research Committee from that of the Fundamental Research Committee.

Many of these projects will undoubtedly involve intimate contact with, and cooperation of, the representatives of industry, which may indicate the desirability of attaching them to the appropriate subcommittees of the Industrial Research Committee. However, this is not in any sense a vital distinction, since our major task is to get work done and to make the results available to all those interested.

Literature Committee

A total of 15,000 cards have been collected and classified in accordance with the classification system tentatively approved by the Literature Committee. Abstracts have been made of some 250 important articles published in French, Spanish and Italian, which were available in the Engineering Societies Library, or otherwise readily obtainable. The results indicate that there is a great deal of information in the literature on important technical phases of welding much of which is not generally available in this country.

A critical digest of the world's welding literature is well under way through the cooperation of the University professors associated with the work of the Fundamental Research Committee, and industrial research workers associated with the work of the Industrial Research Committee.

Reviews have already been made and published on specific phases of the welding field, namely, Bend Tests for Welds, Impact Resistance of Welded Joints, Fatigue Strength of Welded Joints, Welding of Copper and Its Alloys, and Welding of Cast Iron. Other reviews practically completed include those on Corrosion Resistance of Welded Joints, Welding of Cast Steel, Welding of Malleable Iron, Welding of Alloy Steel Castings. Three or four others are in process of preparation. It is proposed that arrangements be made with a number of professors for making continuing reviews of specific phases of welding literature for which they would be paid small sums. There are prospects of getting substantial cooperation from some of the foreign institutions in this review work.

Experimental Research Projects

The impracticability of sharply dividing the work of the Welding Research Committee among the three divisions indicated is clearly brought out in the matter of research projects.

The critical digest of the literature on Fatigue of Welded Joints indicated gaps in knowledge, and the desirability of making some investigations on large-size specimens in this country. A fellowship has been established at Cornell University by the Structural Steel Welding Research Committee of the AMERICAN WELDING SOCIETY and another large comprehensive program is contemplated at the University of Illinois under the guidance of Professor W. M. Wilson, one of the university research workers. This latter program will probably call for the raising of some $25,000 or $30,000, and will be participated in by such organizations as the U. S. Bureau of Public Roads, the American Railway Engineering Association, the Chicago Bridge & Iron Works, the Manitowoc Shipbuilding Corporation and others.

The possibility of conducting a comprehensive program on the fundamentals of resistance welding, such as securing information on optimum currents, time, pressures, size and shape of electrodes for various materials, has been enhanced through the offer of one university to purchase $12,000 worth of equipment, if a research fellowship might be established in cooperation with the automotive and refrigerator manufacturers. There is also prospect of establishment of a large program of investigation on the carbon manganese ratio, and another on the methods for determining the weldability of steel.

Respectfully submitted,
COMFORT A. ADAMS, *Chairman*

THE ENGINEERING FOUNDATION
WELDING RESEARCH COMMITTEE

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Stress Distribution in Fillet Welds—A Review of the Literature to January 1, 1937

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SUMMARY

Elementary and Empirical Formulas

Design formulas for welded connections have been obtained from extensive series of static tensile tests of fillet welds. In general the results show that the strength of end and side fillet welded joints increases approximately in direct proportion, with the dimensions of the throat (for relatively short standard flush 45° fillets), and that end fillet joints are perhaps up to 30% stronger than side fillets of the same throat cross section.

Methods for computing design stresses may be based on load per unit length or on average unit stress in the weld. The leg or the throat dimension (whichever gives the lower capacity) is used in conjunction with experimental values of unit strength.

End Fillets.—Design formulas for end fillets have been deduced on various assumptions concerning the direction of the resultant load on the weld and the effect of eccentricity of load.

Discussion.—The elementary formulas closely predict the static tensile strength of as-welded symmetrical side and end fillet welds of dimensions that can be handled in the laboratory. However, without experimental confirmation, it is perhaps unsafe to apply the formulas for the usual dimensions and types of fillets to unusual values. This fact is evident especially in the case of formulas derived on purely theoretical bases, when one considers the various assumptions (not justifiable) made in their derivation, namely: (*a*) truly elastic deformations, (*b*) identical properties of plate and weld metal, and perfect bond between these, (*c*) absence of shrinkage stresses, (*d*) existence of two-dimensional stress.

Experiments on Stress Distributions

Extensometer Method.—The majority of investigators have studied stress distributions in fillet welds by measuring the strains at important points in a metal model. In the majority of cases a Huggenberger or Marten's mirror extensometer was used. From strain readings at a point in two or three 45° directions, stresses are computed by making use of the elastic constants of the materials tested. The important results are summarized in graphical form in Fig. 8. The major portion of the load is carried by the ends of a side fillet. In end fillets the stress is maximum at the root (up to 3 times average) and in the middle of the length (about 1.5 to 3 times average stress).

Photoelastic Method.—By using polarized light and transparent models, stress distributions in various types of welded connections of modified two-dimensional type have been obtained. Quantitative results have been obtained mainly for end fillets, results for which are shown in Figs. 10 and 11. The maximum stress concentration (Table 4) at the root of end fillets varies from 2 to 10 depending on type of joint, contour of weld and penetration.

Rubber Model Method.—Only qualitative results have been obtained by this method by studying the deformations (after the application of load to the rubber model) of a network of originally straight orthogonal lines.

Brittle Coating or Maybach Method.—This is a rapid

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method for determining the regions of maximum tensile strain in a metallic model under stress, by employing a brittle coating, such as whitewash or special varnish, applied to the surface of the model tested. There has been little use for this method for fillet weld studies.

Fatigue Tests.—Points of maximum stress concentrations in fillet welds are sensitively detected by fatigue tests. Stress concentration factors as obtained by photoelastic methods are indicative of points of weakness, where a crack may be initiated in fatigue tests. The fatigue strength of a joint is also greatly affected by welding defects. Little research has been carried on by this method, unfortunately.

Other Methods.—A great many ingenious methods of more or less practical value have been employed in the study of stresses in fillet welds, such as etching reagent process, glued paper models, net of cotton threads, rubber diaphragm and hydraulic analogy.

The Theory of Elasticity Applied to Fillet-Welded Joints

Those who have attempted to determine the stress distribution in fillet welds with the aid of the theory of elasticity have found that the problem is complicated by an excessive number of variables. However, the stress distribution along the length of the side fillet and that in the cross section of the end fillet is known with some certainty.

Side Fillet-Welded Joints.—This type of joint has provoked a large number of theoretical investigations, and their results are in fair agreement. Almost all investigators assume that the stress distribution obeys a law involving hyperbolic functions. Arnovlevic[95] (1910) analyzed the ideal joint shown in Fig. 12, assuming that the shear force acting at any point on the area of contact of the two concentric bars of different materials is proportional to the relative displacement, the proportionality constant being known as the detrusion ratio. He also presupposed that only shear forces acted on the surface of contact. The application of analytical methods akin to those of Arnovlevic to side fillet-welded joints was made in 1928 by Troelsch.[96] The uniting prisms of weld metal were assumed to be rectangular in cross section and the stress distribution on any cross section of strap and main plate was assumed to be uniform. The main difficulty with this investigation was the detrusion ratio, to be determined experimentally. The problem of non-uniformity of stress distribution across a transverse section through straps and plate was solved by Weiskopf and Male,[53] who showed that the center line of the straps remains a straight line after deformation. Also, they attacked the problems of the internal behavior of a weld of triangular cross section when subjected to shearing forces on the two legs. The approach to the side fillet problem through the detrusion ratio has also been made by others who arrived at equations of the cosh type for shear stress distribution. MacKay and Bain[56] found that a good value of the detrusion ratio was 0.40 E, where E is Young's modulus. Hovgaard[99] proposed to replace the detrusion ratio by a displacement coefficient, (0.31×10^{-7} for mild steel), called by Neuber[103] the shear modulus of the weld. Kalina[100] assumed a uniform distribution of shear stress over the throat of the weld, and that the stress problem was consequently uni-axial. Callandreau[101] used the strain-energy theory and principle of least work in the derivation of his equation. Neuber[103] also studied the single-lap side fillet, and developed a cosh equation, in which he assigned a value of 0.25 E for the shear modulus of the weld.

Rivet Formulas.—An approach to the problem of stress distribution in side fillets has been through similar formulas for riveted joints, assuming that a continuous riveted joint acts like a welded joint. It was found that the stress concentration factor decreases as the length of the weld increases, although the opposite result has also been achieved. There appears to be little justification for assuming that a weld is the limiting case of closely spaced rivets, or vice versa.

Other Theoretical Equations.—The stress distribution in symmetrical double-strap side fillet welded joints was analyzed by Rosenthal,[111] who assumed that at any point the shear strain was proportional to the normal stress at that point. Goelzer[113] investigated the case of a single side fillet weld of finite length assuming that the applied load acts along one shear face of the weld. Also, it was assumed that the shear stress in the cross section of the weld is maximum along the throat. Gerbeaux[114] attacked this problem by assuming an exponential law of shear distribution. Interesting developments along this line have been made also by Kohl,[115] Gardena[118] and Chassaing,[119] besides many others.

The Side Fillet Weld in General.—Two theoretical investigations of the side fillet weld in general have been made. In 1928, Gillespie and coworkers[120] considered a side fillet weld to be equivalent to a thin web joining the two members as shown in Fig. 16. This arrangement of webs and bars permits the application of Filon's equation for shear due to nearly opposite loads, the webs being considered as beams of great depth compared with the distance between loads. The case of the side fillet weld in connecting a reinforcing strap to a solid plate has been exhaustively investigated by Hovgaard and coworkers.[123] A slotted plate with a web welded in by means of continuous side fillets was also studied. Ohno[124] applied Troelsch's formula to a similar problem and arrived at an equation of the familiar cosh form.

End Fillet-Welded Joints.—Comparatively few attempts have been made to determine the theoretical stress distribution in end fillet welded joints, because of the difficulty of the problem. In 1925 the German National Materials Testing Laboratory[24] investigated this problem as a plane problem in bending. Many investigators have attacked the problem in more or less different ways without successful results, as compared with experimental (photoelastic) investigations.

Other Types of Fillet-Welded Joints.—The stress distribution in box-welded joints has been analyzed on the assumption that the outer end of the side fillet must undergo the same deformation as the entire end fillet. The equations derived are of the cosh type, and show that the end fillet cannot be regarded simply as an additional length of side fillet. An analysis of the forces in T-joints and in wedge-shaped combined butt and end fillet welds has been made.

Fillet Welds in Bending

Experimental.—Very little has been done in determining the behavior of fillet welds subjected solely to bending, or for that matter, to torsion. It has been found that up to the elastic limit the ordinary design formulas give the average stress condition in the weld. Once the yield point is exceeded the difference between the stresses at the face and root of the weld decreases. The stress is practically uniformly distributed over the cross section of the weld when inelastic action has occurred to an appreciable extent.

Theoretical.—Theoretical investigations of moment-resisting fillet-welded joints have been made on the basis of linear distribution of bending stress in the strap, the cosh equation again being encountered. In moment resisting box welds the maximum shear stress in the side fillet may be expected at the free end.

Stress Distribution in Fillet Welds

Introduction

THE strength of fillet welds of all kinds depends primarily on three factors:

1. The Materials Factor; that is, the materials of which the plates and welds are made. The literature contains data on the static and dynamic tensile strength (and, to some extent, bending strength) of fillet welded joints in structural steel and, to a limited extent, in wrought iron.

2. The Welding Factor; that is, the effect of penetration, crater, machining and number of layers. This factor has been studied in connection with fatigue investigations, particularly, but has also been touched upon in static tensile studies.

3. The Design Factor; that is, the dimensions and geometrical shape of the joint and weld. The design factor involves most of the variables of stress distribution proper, such as the ratio of distance between side fillets and thickness of plate, and the type of joint, whether end, side, box, plug or inclined fillet.

Of these three factors, which are the factors that must be considered if the mechanical properties of a fillet weld are to be improved, the present review deals only with the second and third. Although it is true that the stress-strain properties of materials have an effect on stress distribution, the factor is relatively insignificant for the customary metallic materials. Besides, there is very little information on the behavior of fillet welds in materials other than mild or structural steel, beyond the fact that the behavior of fillet welds in laminated materials depends on the direction of rolling of the material with respect to the applied load. The time is not ripe for a review of the materials factor.

The second factor involves several variables affecting stress distribution in actual joints, namely: (1) shape of the weld, whether concave, flush or convex; (2) transition between plate and weld, whether gradual, abrupt or undercut; (3) distribution of passes along the length of the weld; (4) clamping force due to welding distortions; (5) character of surface and ends of weld, whether machined or as-welded and (6) penetration. Quantitative experimental and theoretical investigations of several of these variables have been made, and are discussed in the present review.

The third factor involves the design or geometrical aspects of the joint about which most of the investigations of fillet welds have centered. It is the large-scale stress distribution which is mainly included in this factor, whereas the welding factor involves mainly small-scale or local stress distribution. Nevertheless, items 1 and 2 listed under the welding factor are also under the designer's control.

In summarizing the literature on fillet welds it has been thought best to deal first with the elementary formulas based on strength of materials and empirical formulas based on experimental tests of actual joints. These formulas and tests serve as a natural introduction to more refined experimental methods of determining the stress distribution in actual joints or models thereof. The stress distribution determined experimentally naturally invites mathematical representation, the investigations of which are summarized in the later sections of the review. Throughout, the viewpoint of the reviewers has been to classify methods and to identify and separate variables, as well as to suggest relative authoritativeness of results. No attempt has been made to deal with the principles of welded structural design, which have been discussed by McKibben,[1] Priest,[2] Vogel,[3] Fish[4] and others.

Elementary and Empirical Formulas

Since the outlook of strength of materials and materials testing embodied in the deduction of elementary or empirical formulas for the strength of fillet welds cannot give quantitative expression to the notion of stress distribution, the numerous tests of the static properties of fillet welds and the customary design formulas will be dealt with only briefly in the present review. Extensive series of static tensile tests of fillet welds have been made by:

Höhn[5] (empirical formulas for the strength of end and double-strap side fillet welds),
Bryla[6] (empirical formulas for side fillets; strength depends on leg, not on throat, of welds),
Grove,[7] Vogel,[8] Humphrys,[9] Bibber[10] (machined end fillets, $1/8$- to $1\,1/4$-in. leg),
Davis and Carpenter[11] (end 30% stronger than side based on equal throats),
Hobbs[12] (end and side, $1/8$- to $5/8$-in. leg, single strap, end 20% stronger than side),
Freeman[13] (side, end and inclined fillets),
Patton and Shevernitsky[14] (effect of length of side fillet on strength),
Moon[15] (strength is proportional to thickness of fillet measured from inner corner along center line of section of weld),
Griffin[16] (end and side, $1/4$- to $3/4$-in. leg, end 20% stronger than side),
Hallström[17] (side, end and box fillets),
Reeson[18] (end lap fillets),
Dustin[19] (end and side fillet welds in rolled angle and T-sections),
Wilson[145] (models of lap welds machined from $3/4$-in. mild steel had about the same strength as arc-welded lap joints),
Wadling[144] (load-deformation test on interrupted welded lap joint, three 1-in. end fillets, incomplete details),
Brown[20] (end, side and plug fillets are equivalent in strength; strength per inch of fillet does not increase as fast as volume of weld metal per inch of fillet; working stress at throat is 6000 psi for convex welds, 7000 psi for concave deposits),
Lobban[21] (single-lap, end fillet, double-welded joints and models thereof machined from solid mild steel with and without slots),
Lagrange and Rosenthal[22] (strength of double-strap side fillet weld depends on the angle made by the ends of the cover plates with the main plate; strength is maximum when the cotangent of this angle is 0.4),
Duczinski[23] (side fillets, strength is reduced by thicker fillets, but not by longer fillets),

Neese[24] (side, end and box fillets using angle sections as straps),
Kayser[25] (effect of location of weld in side fillets),
Structural Steel Committee[26] (single- and double-strap end and side fillet joints),
Adams[27] (double-welded lap joints, single- and double-strap reinforced butt welds),
Moss[28] (gas-welded double-strap side fillet joints; yielding starts at the ends of the welds),
Wallin and Schade[29] (joggled joints),
Blackwood[30] (double-strap, arc-welded side and end fillets),
Couzin[31] (static shear strength of side fillet welds in mild steel is 80% of rolled mild steel),
Marsh[32] (summarizes several unpublished investigations prior to 1928).

The list is admittedly incomplete but it is believed that other published results not included in the list add little additional information. A critical discussion of the experimental tests, which are contradictory in some respects, will not be attempted because no useful purpose would be served. It is generally true that end fillets are stronger but less ductile than side fillets of equivalent throat section. Well-established design formulas are known to give entirely reliable results in practice.

Side Fillets

The methods for computing design stresses have been reviewed by Jennings,[33] Jensen,[34] Vigener and Rüdel,[35] Tatur and Rykalin,[145] Schmudde,[36] Haas[37] and Rossell,[38] the last giving a summary of experimental results as well. According to Jennings, the throat area in shear is used for computing side fillets. Seely's and other textbooks also adopt the allowable shear stress and the throat cross section as the basis of design. The German specifications also use the stress basis, but Jensen[34] and the Polish and Hungarian specifications favor the use of a permissible shear per unit length.

Bryla[6] proposes a formula for allowable shear in side fillets which permits short fillets to carry higher stresses than long fillets. For side fillet-welded round reinforcing bars Lucan[39] based design on length and arc of contact of weld. Nomographs for designing fillet welds have been prepared by Griffith,[40] who does not describe his methods of computation.

End Fillets

For 45° end fillets the throat stress in lb. per sq. in. (psi) is 1.618 P/LT, where L = length of weld in inches, T = leg of weld (not throat) in inches and P is the applied load in lb. This formula was developed in the Technical Report (1928) of the B. E. B. & E. I. Co., pages 26–28, and was considered superior to 1.414 P/LT or 2 P/LT (developed by Bibber,[10] who obtained nearly perfect agreement with experimental results, if the tensile strength of weld metal was suitably chosen). In his 1936 paper, however, Jennings[33] adopts the formula 1.414 P/LT without explanation. Bibber[41] states that the tensile stress in end fillets varies as $1/\cos^2 A$, where A is the angle between main plate and face of weld, on the assumption that the stress acts parallel to the face of the weld. In Michel's[42] opinion the leg of an end fillet weld in contact with the main plate should be 1.414 times as long as the leg in contact with the cover plate. The cover plates of end fillet welds should be 10% thicker than the main plates to obtain maximum strength, according to Haas.[43]

Höhn[5] and Pinczon[44] give empirical formulas for end fillets. The latter's formula for lap joints, double-end fillet welded is:

$$P = \frac{2\,S_w L(A - 0.1T)}{1 + \left(\frac{T}{R + 3A}\right)}, R \leqslant 4T,$$

where P = load at fracture, kg.
S_w = tensile strength of all-weld-metal, kg./mm.
L = length of one weld, mm.
A = throat, mm.
R = length of overlap, not including weld, mm.
T = thickness of each plate, mm.

In addition to purely empirical curves and formulas based on experimental results, Höhn bases computation of end fillets on the bending effect, arriving at the formula

$$S_B = \frac{12\,PB}{LH^2}$$

where S_B = breaking stress
B = lever arm of load R about root of fillet
P = applied load
L = length of end fillet weld
H = leg of end fillet on cover plate.

The simplifications involved in Höhn's work are clearly pointed out by Kochendörffer.[45] Hakin's[46] assumption that the resultant acts at the center of pressure of the loaded contract area of an end fillet, that is, at a distance of $^2/_3$ the length of the leg from the root, has no experimental support. Curiously enough, he considered the eccentricity about which the resultant exerts bending moment as the distance between the neutral axis of the critical plane (throat) and the line of action of the resultant. Pigeaud[47] assumes that the maximum stress in symmetrical double-strap end fillets without slots, due to bending is twice the average shear stress calculated on leg area. Bechtle,[48] using standard German methods, bases his design method on the assumption that bending moment is uniformly distributed, as in the usual beam theory.

Asano,[49] and Streletzky and Nikolaieff[50] derive simple formulas for end fillets on the assumption that the fillet weld acts as a fixed, uniformly loaded cantilever beam of varying cross section. The latter formula is

$$S = 3P\,(L/K)^2$$

where S = uniform stress on surface of contact of weld with cover plate
P = load in cover plate
L = leg of weld in contact with cover plate
K = leg of weld in contact with main plate.

An elementary design formula for box fillets has been deduced by Rosskopf.[147] As a first approximation he assumed that under a given load the unit deformation of the end fillet is twice that of the side fillet. Consequently, a unit length of end fillet carries twice as much load as unit side fillet. A more usual assumption is that end and side fillets may be added together in computing box fillet joints.

Discussion

It must be said that the elementary formulas closely predict the static tensile strength of as-welded symmetrical end and side fillet welds of dimensions that can be handled in the laboratory. It is realized that, without experimental confirmation, it is unsafe to apply the formulas for the usual dimensions and types of fillets to unusual values. The elementary formulas, of course, give no indication of ductility or origin of failure.

The experimental tests of the static strength fillet welds are complicated by a number of factors. In the

first place, the as-welded specimen (almost all investigators used as-welded specimens for practical reasons) contains shrinkage stresses, which, with other effects, obscure the elastic properties and yield point. As McKibben[51] pointed out, the clamping force between main and cover plates due to distortion during welding may affect stress distribution. The effect of changing shrinkage stresses by different welding sequences calls for further investigation. At the breaking load the fillet-welded joint is considerably deformed from its original shape. There are wide differences in microstructure and hence in local mechanical properties in as-welded fillets. In annealed, unmachined specimens the variation of microstructure in weld and adjacent metal is slight, but notches due to faulty, irregular penetration, as well as craters, and undercut affect the strength and location of fracture. Probably annealed, machined fillet welded specimens give the best indication of the intrinsic strength of the joint provided distortions due to welding, heat treatment or machining are absent. Models machined from solid plate also provide interesting information, but have seldom been used.

Experiments on Stress Distribution

Most of the common methods of demonstrating the distribution of stress in elastic bodies have been applied to fillet welds. The majority of investigators have employed the extensometer method on welded joints or steel models, conversion of strain to stress being carried out usually with the aid of Young's modulus. It has been found impossible, and indeed unnecessary, to give a full summary of the work of each investigator. Therefore, only the scope and principal results are given for each investigation.

Extensometer Method

The investigations are summarized below in chronological order.

E. Höhn[5]—1925

The large scale stress distribution in fillet welds of all types has been determined by Höhn by means of extensometers. Experimental details are not given in full but strain measurements were made at intervals of 45° around a point and reduced to normal and shear stresses by the method of Wyss. Typical stress distribution charts for box welds are shown in Fig. 1. Höhn has prepared similar charts for end and side fillets and for reinforcing straps on solid plate and over butt welds. The outlook Höhn adopts as a result of his measurements is shown in Fig. 2. At some point along a side fillet weld the stress in plate equals the stress in straps. The relative distortions measured from this point as base determine the strength of the joint.

D. Rosenthal[52]—1929

Huggenberger extensometer measurements on a symmetrical double-strap side fillet welded joint were reduced to normal and shear stresses using Young's modulus and Poisson's ratio. There were considerable differences between the stresses determined in one strap and those measured in the other.

W. H. Weiskopf and M. Male[53]—1930

The longitudinal strain in side fillet welds is measured by means of an apparatus designed and first employed by J. H. Smith.[54] The shear is a minimum at the middle of the length of weld and a maximum at both ends.

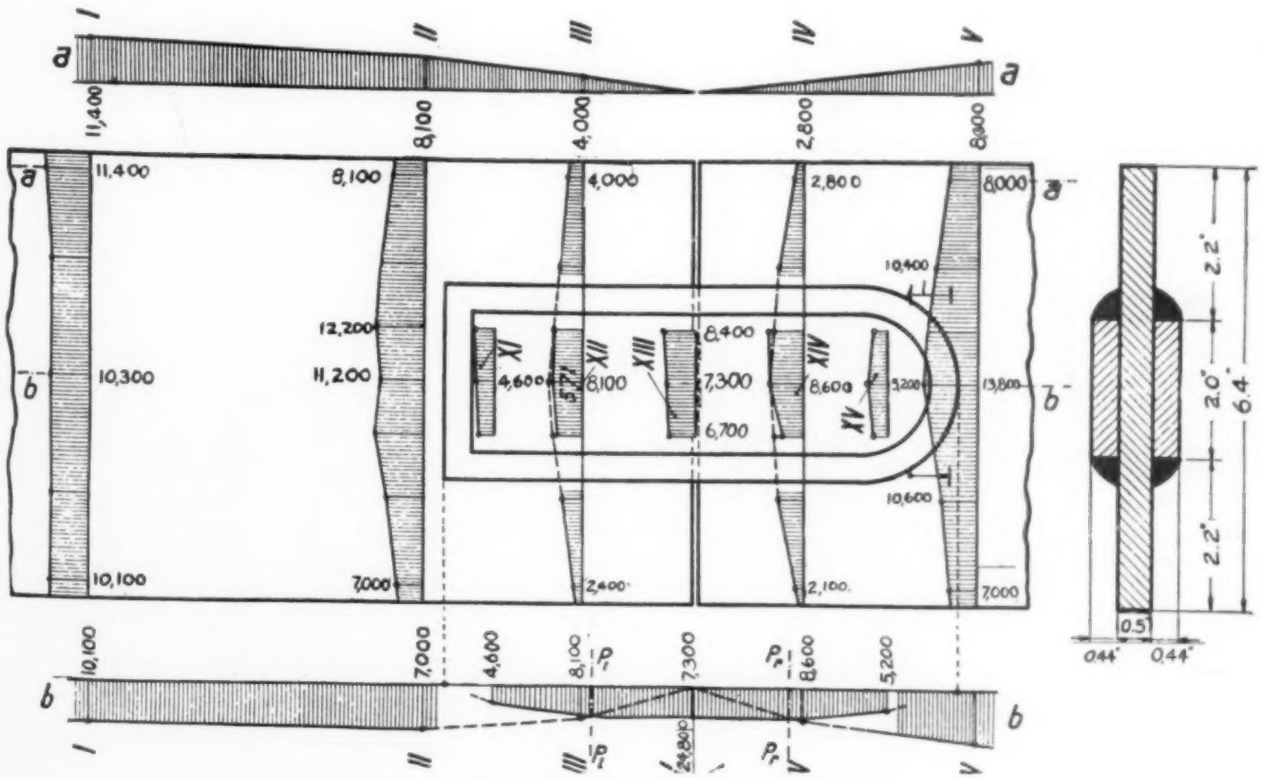


Fig. 1—Observed Stress Distribution in Box Fillet Weld

Applied load = 33,000 lb.
Stress in lb. per sq. in.
Mild Steel, arc welds. Höhn[5]

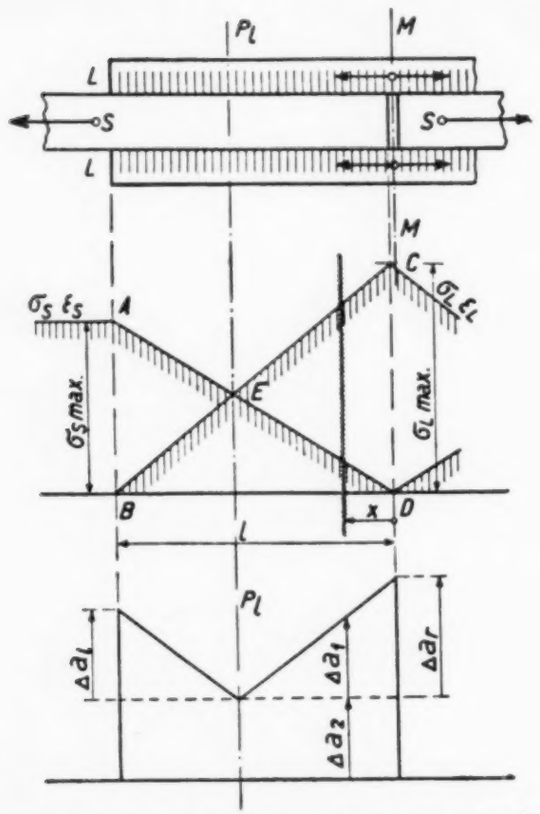


Fig. 2—Middle Diagram Is a Schematic Diagram of Stress Distribution in Plates and Straps of a Side Fillet Joint. Lower Diagram Shows Relative Internal Displacement Δa_1 (Maximum Values and Δa_r) and External Displacements Δa_2

σ_s = normal stress in plate
ϵ_s = elongation in plate
σ_l = normal stress in straps
ϵ_l = elongation in straps. Höhn[5]

D. ROSENTHAL[55]—1930

Axial deformations across three sections of box welds with four different ratios of length of side fillet (= d) to length of end fillet (= a) are measured by means of Huggenberger extensometers. The results are shown in Figs. 3 and 4, in which x is the distance from the end fillet weld to the measured section. The plot of the ratio: shear stress in side fillet to normal stress in end fillet for varying $\frac{d}{a}$ is linear. In computing box welds it is not permissible to add the lengths of side and end welds.

H. M. MACKAY and A. M. BAIN[56]—1930

Measurements of strain in side fillet welds with the aid of Martens mirror extensometers showed that the load P_1 transmitted by the fillets per unit length is directly proportional to the relative displacement. Extensometer readings were made at 1-in. intervals along the welds, which were $6^1/_2$ and $9^1/_2$ in. long. The ratio of maximum to average P_1 was 1.87 for the $6^1/_2$-in. specimen and 2.26 for the $9^1/_2$-in. weld.

J. R. GRIFFITH[57]—1932

The strain distribution was measured for end fillet welded straps in the form of angle sections. The weld itself was not studied. Eccentricity due to tensile load created compression in one leg of the angle.

S. C. HOLLISTER and A. S. GELMAN[58]—1932

A determination is made of the stress distribution in welds and plates of double-strap side and box fillet welded joints in mild steel by means of Huggenberger extensometer measurements ($^1/_2$ in. gage length). Axial and lateral strains were measured and reduced to stresses by means of Young's modulus and Poisson's ratio.

E. GABER[59]—1932

A Martens extensometer was used to determine the stress distribution in side fillet welds in mild steel at an average stress of 10,000 psi. Stress was computed directly from strain measured parallel to the axis of tension, and expressed as $^1/_2$ $(d_o - d_u)$, where d_o is strain at edge of strap, and d_u is strain in adjacent plate, both related to gage length. The ratios of shear stresses in the weld at the outer end, middle and inner end are given in Table 1.

Table 1—Ratios of Shear Stresses in the Weld of a Side Fillet Joint at the Outer End, Middle and Inner End. Gaber[59]

Length of Seam, In.	Welded	Welded and Strengthened by Rivets
1.18	1.27:0.89:1.15	1.33:0.86:1.19
2.36	1.82:0.68:1.50	2.06:0.63:1.55
3.53	1.79:0.69:1.62	2.79:0.43:1.83

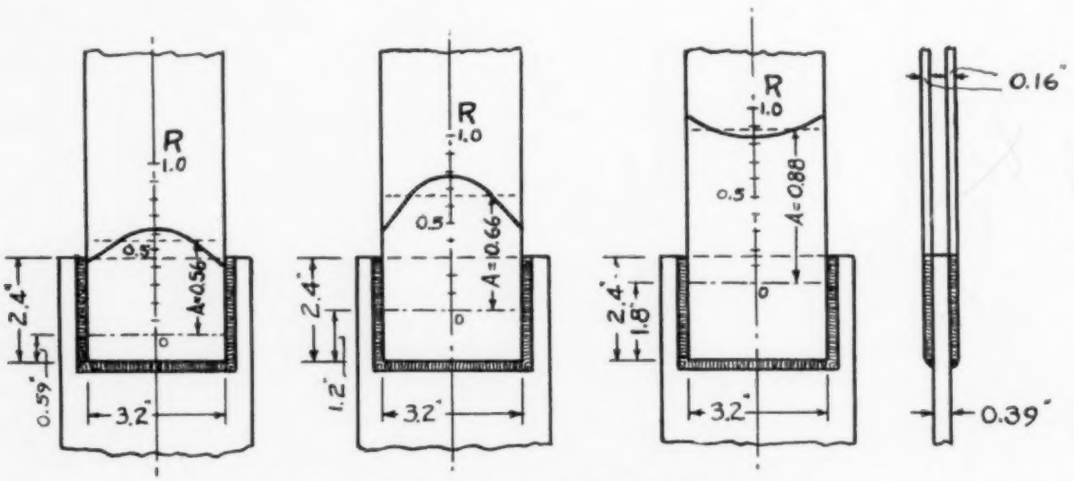


Fig. 3—Distribution of Strain in a Box-Welded Joint

R = Ratio of elongation of strap outside joint in the direction of load divided into the elongation of the strap across the given section within the joint
A = Average value of R across the given section. Rosenthal[55]

G. BIERETT and G. GRÜNING[60]—1933

Extensometers (0.39 and 0.78 in gage lengths) were used to map the distribution of shear and normal stresses and the principal stress trajectories in large models of symmetrical, double-strap end fillet-welded joints machined from mild steel, 0.42 in. thick, Fig 5. The angle between the hypotenuse of the model weld and the shear leg was 30 or 45°. The slots (0.1 in. wide) in the joints were either left unpacked, or packed with strips. The average tensile stress in section *AB* (6.3 in. wide) was 10,700 psi. Measurements on the upper and lower surfaces were averaged. The results expressed as multiples of the average stress are given in Table 2. More detailed results are given by Bierett and Grüning in seven stress distribution diagrams.

The extensometer measurements were made in three directions at a point (ellipse method) and were checked by measurements in a fourth direction. The packing absorbed 40% of the tensile load in the 45° joint, but only 24% in the 30° joint. Pulsating tension fatigue tests of arc- and gas-welded specimens of the same shape as the models showed that fatigue failure starts at the points of maximum stress concentration. Bierett and Grüning also studied a T joint.

G. BIERETT[62]—1933

The results of extensometer measurements (small gage lengths) on symmetrical side fillet welds parallel and perpendicular to the axis of load are shown in Fig. 6. The stress problem is regarded as three-dimensional, the load being carried by shear stresses along the edges of the plate and strap, which are highest at the ends. The maximum stresses calculated from the extensometer measurements are smaller than the actual maximum stresses, which are produced by the longitudinal and transverse normal stresses and the shear stress on the legs of the welds. Inelastic deformation occurs when the maximum shear stress exceeds one-half the yield stress. The curve of resistance to deformation (plotted as stress parallel to the surface of the plate *vs.* strain in the direction of the maximum principal normal stress) of side fillet welds lies midway between the corresponding curves for bolted joints and a machined, solid tensile specimen.

E. O. PATTON, M. I. KOZLOWSKY and V. V. SCHEVERNITZKY[63]—1933

Stress-strain curves were determined for the straps of several side fillet-welded joints employing angle sections as straps.

D. ROSENTHAL[64]—1934

Huggenberger extensometer measurements on end fillet welds show that the distribution of stress across

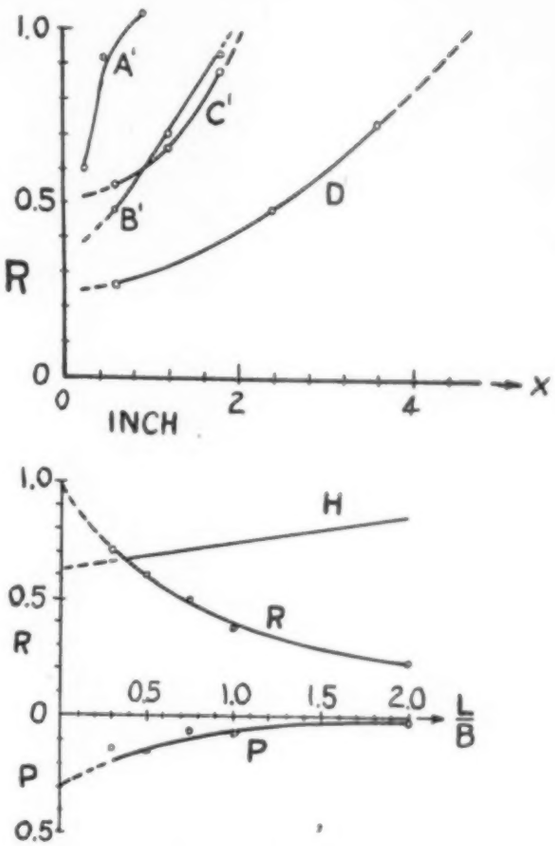


Fig. 4—Upper Diagram. Variability of the Mean Deformation in Box Fillet Welds

x = Distance along side fillet measured from end of strap

R = Ratio of elongation of strap outside joint in direction of load divided into elongation of strap across the given section within the joint

$$A' - \frac{L}{B} = 0.5$$

$$B' - \frac{L}{B} = 1.0$$

$$C' - \frac{L}{B} = 0.75$$

$$D' - \frac{L}{B} = 2.0$$

L = Length of one side fillet

B = Breadth of strap (length of the end fillet). The curves are extrapolated to $x = 0.20$ inch.

Lower Diagram. Proportion of Load Carried by the End Fillet of a Box Fillet Joint

H = Ratio of average shear stress in side fillets to average normal stress in the end fillets

$$H = \frac{1}{R} - \frac{1}{2}\frac{L}{B}$$

P = Ratio of cross sectional contraction of strap at a given section within the joint to the elongation of strap outside joint in direction of load. Rosenthal[65]

Table 2—Stress Concentrations in End Fillet-Welded Joints.
Bierett & Gruning[60]

Point	45° Weld						30° Weld					
	Slots Empty			Slots Filled			Slots Empty			Slots Filled		
	$S_{max.}$	$S_{min.}$	$T_{max.}$	$S_{max.}$	$S_{min.}$	$T_{max.}$	$S_{max.}$	$S_{min.}$	T_{max}	$S_{max.}$	S_{min}	$T_{max.}$
0.39 in. above *B* on *AB*	2.38	−1.48	1.86	1.03	−0.16	0.59	1.61	−1.08	1.34	1.26	−0.29	0.77
0.39 in. from *B* on *CB*	0.51	−2.81	1.66	0.89	−0.35	0.62	0.38	−2.13	1.25	0.98	−0.73	0.86
Extrapolated to *B'*	2.8	0.0	1.4	1.1	0.0	0.55	1.8	0.0	0.9	1.4	0.0	0.7
Extrapolated to *B''*	0.0	−3.8	1.9	0.0	−0.5	0.25	0.0	−3.2	1.6	0.0	−1.4	0.7
Extrapolated to *B*	2.8	−3.8	3.3	1.0	−0.5	0.8	1.8	−3.2	2.5	1.4	−1.4	1.4
0.39 in.[1] from *C* on *CB*	3.10	0.40	1.35	2.30	0.32	0.99	1.84	0.14	0.85	1.70	0.12	0.79
0.39 in. below *C* on *CD*	2.82	0.77	1.02	2.08	−0.60	0.74	1.86	0.30	0.78	1.70	0.34	0.68
Extrapolated to *C*	3.9	0.0	1.95	3.0	0.0	1.5	2.1	0.0	1.05	2.0	0.0	1.0

[1] 0.59 in. for the 30° joint.

$S_{max.}$ = maximum normal stress at the assigned point
$S_{min.}$ = minimum normal stress at the assigned point
$T_{max.}$ = maximum shearing stress at the assigned point

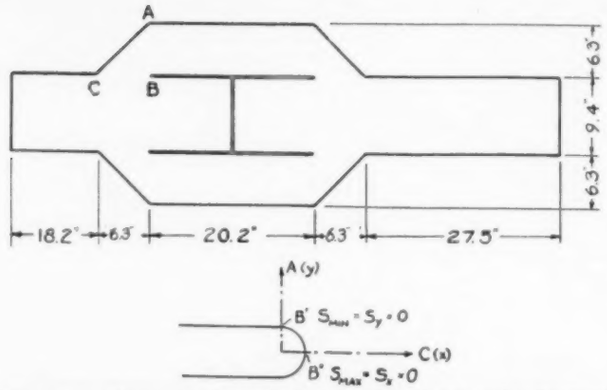


Fig. 5—Upper Diagram. Steel Model of End Fillet-Welded Joint Used by Bierett and Gruning[60]

Lower Diagram. Analysis of Stresses at Point B

the line of junction between strap and weld is nearly a linear function of distance along the junction, being zero at the toe and maximum at the root.

Extensometer measurements also showed that the stress distribution is not uniform over the length of a box fillet weld, as shown in Fig. 3. The maximum strain perpendicular to the welds of a double strap end fillet joint (straps 0.4 inch thick, 2.35 in. wide) was 50% greater along the center line of the strap $1/2$ inch from the weld than at the edges of the strap.

HOEFFGEN[65]—1934

Martens mirror extensometer measurements were made on symmetrical, double-strap side fillet welds (gas and arc in mild steel). It was found that, for any two points directly opposite each other along the edges of the side fillet weld, the displacements were proportional to the applied load, so that strain could be directly converted to stress. Measurements were made at five points along the seams and are summarized in Table 3 for gas welds at an applied stress of $P/4A1$ = 13,000 psi. The values are given in per cent of $P/4A1$. The results for arc welds were almost the same as for gas, which suggests that shrinkage stresses have little effect on stress distribution.

Table 3—Stress Concentration in Symmetrical Double-Strap Side Fillet Welds. Hoeffgen,[65] 1934

Length of Seam, Inches	1.18	2.36	3.54
Stress at Inner End of Seam, %	119	142	166
" at Middle of Seam, %	89	74	73
" at Outer End of Seam, %	133	172	189

A valuable review of data on stress distribution in fillet welds as revealed by extensometer measurements was made by Bierett[131] in 1933. He summarized results at that time in three diagrams, Figs. 7, 8 and 9. Figure 7, based on Gaber's results, which are considered most accurate experimentally, shows that the longer the weld, the better the stress distribution in side fillet welds. Gaber's results apply only if plates and straps have about the same cross section, otherwise the stress distribution is less favorable. Figure 8 is based on measurements by Rosenthal (box welds) and the German National Materials Testing Laboratory (side fillet welds). Curve 3 in Fig. 8 is derived from the results shown in Fig. 9. Box welds have better stress distribution than side fillets. Fig. 7 is interesting as an indication of stress concentration in infinitely short welds.

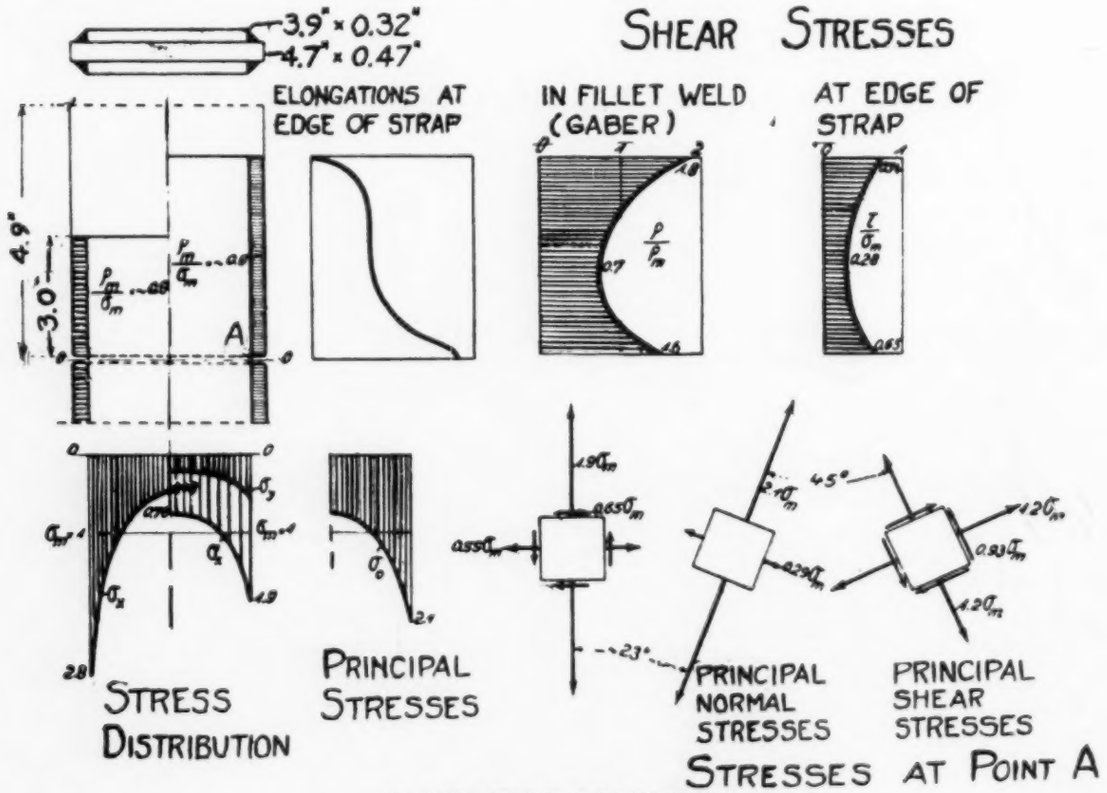


Fig. 6—Stress Distribution in Side Fillet Welds

σ_m = average normal stress
σ_x = normal stress parallel to weld
σ_y = normal stress perpendicular to weld
ρ_m = average shear stress in weld
ρ = shear stress in weld at a given point
τ = shear stress at edge of strap. Bierett[62]

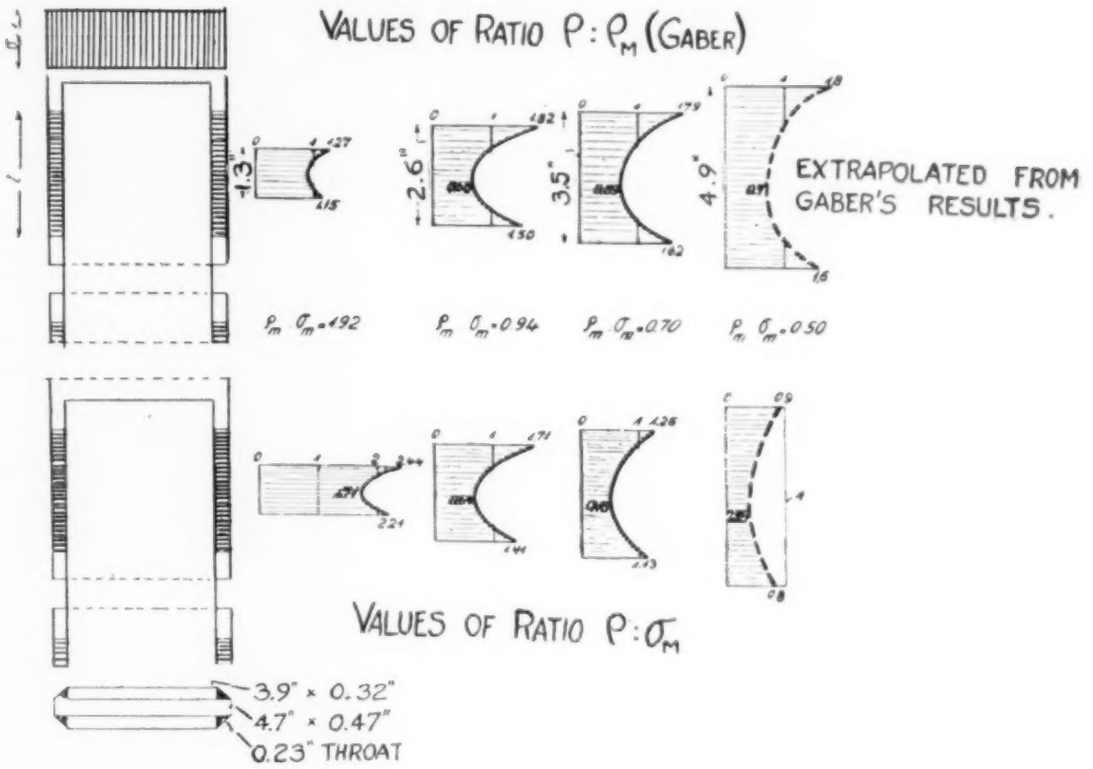


Fig. 7—Distribution of Shear Stress in Side Fillet-Welded Joints

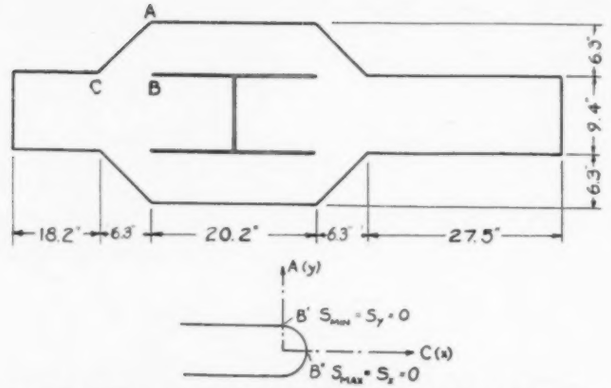
p = Shear stress at a given point
p_m = Average shear stress in fillet
σ_m = Average normal stress in strap. Bierrett[131]

Observation of the failure of actual symmetrical side fillet welded joints under static load cannot be said to confirm the theoretical prediction that failure should commence simultaneously at both ends of the fillets. Inelastic action usually occurs long before failure. In single lap glued joints in hickory, however, the Adhesives Research Committee[149] observed that the first points of failure were always the ends of the overlap, although there was doubt as to the nature of final failure, and indeed as to whether the initial failure was not due solely to the effect of eccentricity in single-lap joints. Tests of solid pieces of hickory in the form of single-lap joints showed that the average shear stress decreased from 2200 to 1200 psi as the shear area was increased from $^1/_2$ to 2 sq. in. and that the average shear stress increased from 1136 to 2226 psi as the overlap of the models was decreased from 1 in. to $^1/_4$ in. Both observations show that the stress concentration factor increases with increase of shear area, which is in agreement with Gaber's results for side fillet welds.

Photoelastic Method

Investigations of fillet welds by the photoelastic method have been made by Male[66] (box fillet welds), Dustin[67] (end fillet weld), Chapman[68] (effects of improper welding), Smith[69] (various welding conditions), Lyse and Stewart[70] (bending in welded seat angle connections), Vose[71] (numerous joints and fillet contours), Baud[72] (T joint), Coker[73] and coworkers (Levi, Russell), (single- and double-strap end fillet joints, collar weld reinforced by single end fillet-welded strap including study of penetration at collar weld, T joint, joggled butt joint), Mesmer[74] (double-strap end fillet joints with convex, concave, flush and relieved welds), Hollister[75] and coworkers (Gelman, Wood) (side, end, box and inclined fillet joints), Solakian[76] (a large variety of end fillet welds, including effect of penetration, as well as stress distribution along end and box fillet welds), Goelzer,[77] (glass models of end fillets, lines of principal stress), Sasaki[78] (no details), Yamanouti, Fuziwara and Sakezawa[150] (single- and double-welded end fillet lap joints with and without overlap and with 30 or 45° welds, symmetrical double-strap end fillet-welded joint, single glued lap joints, T welded joints, and T welded joints between a web plate and a ribbed flange) and Samsonov[79] (end fillet welds). A study of miniature reproductions of lap fillet joints made of glued celluloid and examined under a polarizing microscope by the Adhesives Research Committee[149] (1926) showed that the maximum stress concentration was found at the ends of the overlap when the joints were loaded in tension. Tests of actual glued joints in wood, however, failed to show that a redesign of the joint with rounded fillets at the ends of the overlap had any effect on strength. The result is not surprising in view of the fact that stress concentration at the ends of side fillet welds or glued joints is an inherent property of the type of load transmittal represented by these joints.

Of these investigators only Coker and coworkers, and Solakian provide quantitative information on maximum stress concentration factors. Coker's models were machined from solid celluloid. Slits were $^1/_{16}$ or $^1/_{20}$ in.



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" at Outer End of Seam, %	133	172	189

A valuable review of data on stress distribution in fillet welds as revealed by extensometer measurements was made by Bierett[131] in 1933. He summarized results at that time in three diagrams, Figs. 7, 8 and 9. Figure 7, based on Gaber's results, which are considered most accurate experimentally, shows that the longer the weld, the better the stress distribution in side fillet welds. Gaber's results apply only if plates and straps have about the same cross section, otherwise the stress distribution is less favorable. Figure 8 is based on measurements by Rosenthal (box welds) and the German National Materials Testing Laboratory (side fillet welds). Curve 3 in Fig. 8 is derived from the results shown in Fig. 9. Box welds have better stress distribution than side fillets. Fig. 7 is interesting as an indication of stress concentration in infinitely short welds.

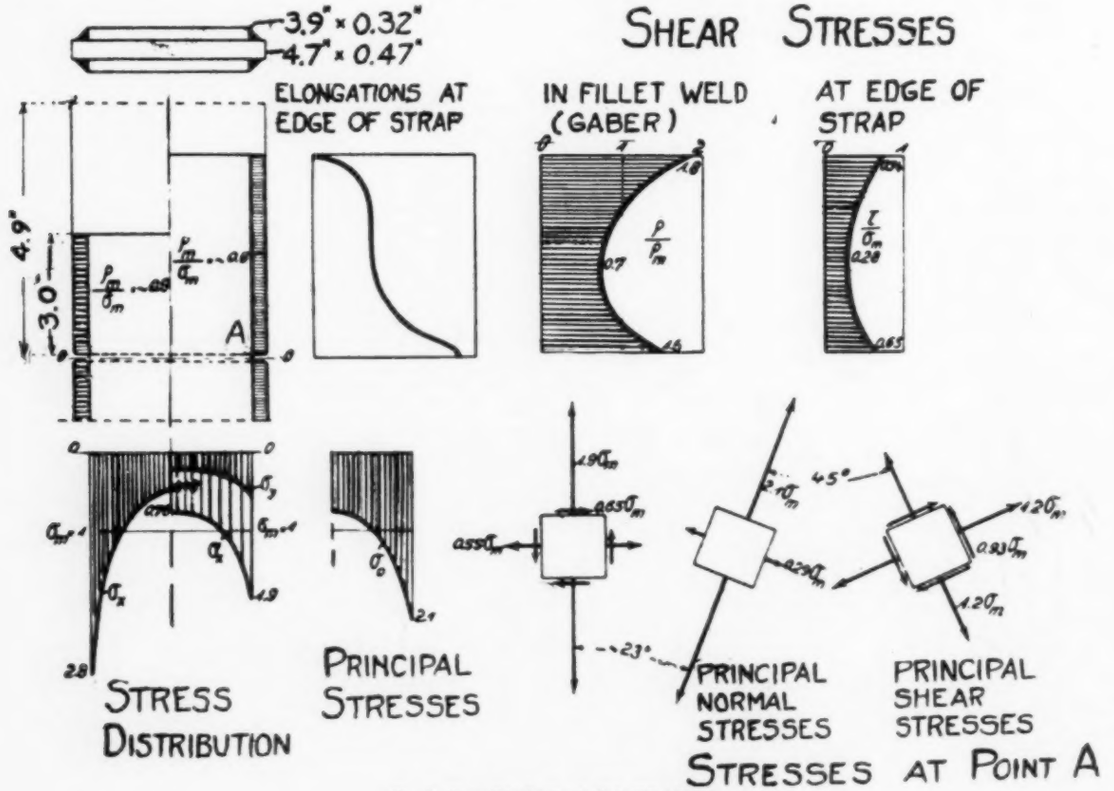

Fig. 6—Stress Distribution in Side Fillet Welds

σ_m = average normal stress
σ_x = normal stress parallel to weld
σ_y = normal stress perpendicular to weld
ρ_m = average shear stress in weld
ρ = shear stress in weld at a given point
τ = shear stress at edge of strap. Bierett[62]

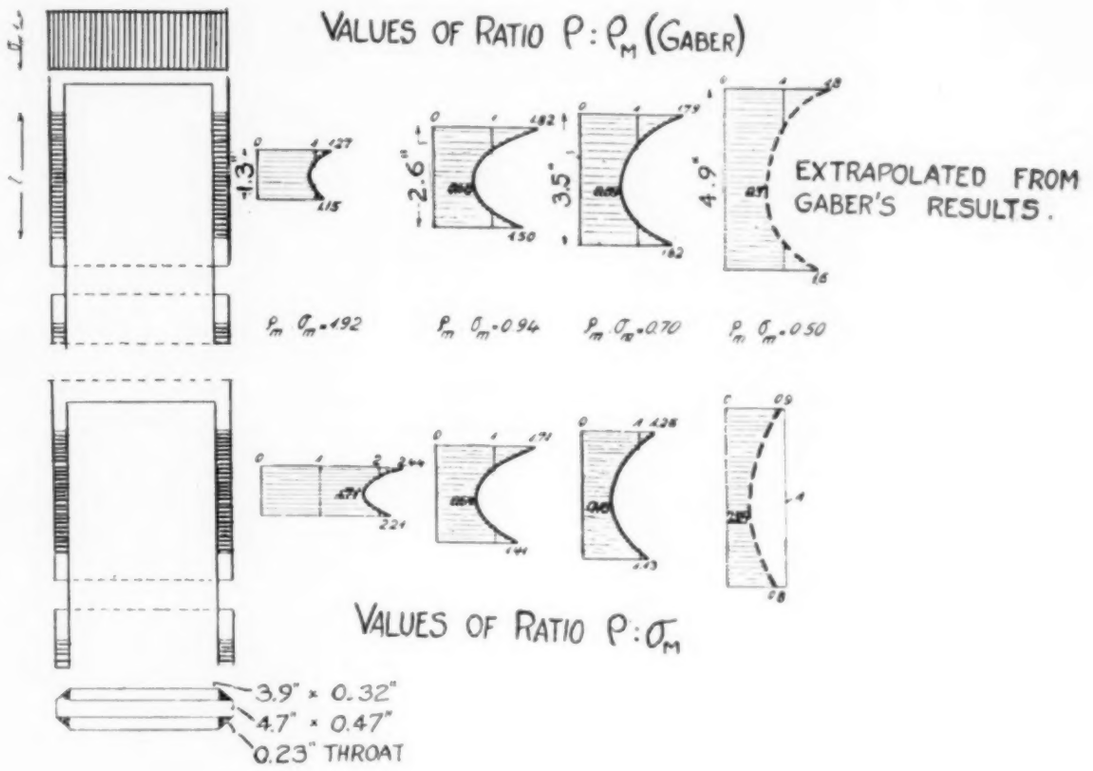

Fig. 7—Distribution of Shear Stress in Side Fillet-Welded Joints

ρ = Shear stress at a given point
ρ_m = Average shear stress in fillet
σ_m = Average normal stress in strap. Bierrett[131]

Observation of the failure of actual symmetrical side fillet welded joints under static load cannot be said to confirm the theoretical prediction that failure should commence simultaneously at both ends of the fillets. Inelastic action usually occurs long before failure. In single lap glued joints in hickory, however, the Adhesives Research Committee[149] observed that the first points of failure were always the ends of the overlap, although there was doubt as to the nature of final failure, and indeed as to whether the initial failure was not due solely to the effect of eccentricity in single-lap joints. Tests of solid pieces of hickory in the form of single-lap joints showed that the average shear stress decreased from 2200 to 1200 psi as the shear area was increased from $^1/_2$ to 2 sq. in. and that the average shear stress increased from 1136 to 2226 psi as the overlap of the models was decreased from 1 in. to $^1/_4$ in. Both observations show that the stress concentration factor increases with increase of shear area, which is in agreement with Gaber's results for side fillet welds.

Photoelastic Method

Investigations of fillet welds by the photoelastic method have been made by Male[66] (box fillet welds), Dustin[67] (end fillet weld), Chapman[68] (effects of improper welding), Smith[69] (various welding conditions), Lyse and Stewart[70] (bending in welded seat angle connections), Vose[71] (numerous joints and fillet contours), Baud[72] (T joint), Coker[73] and coworkers (Levi, Russell), (single- and double-strap end fillet joints, collar weld reinforced by single end fillet-welded strap including study of penetration at collar weld, T joint, joggled butt joint), Mesmer[74] (double-strap end fillet joints with convex, concave, flush and relieved welds), Hollister[75] and coworkers (Gelman, Wood) (side, end, box and inclined fillet joints), Solakian[76] (a large variety of end fillet welds, including effect of penetration, as well as stress distribution along end and box fillet welds), Goelzer,[77] (glass models of end fillets, lines of principal stress), Sasaki[78] (no details), Yamanouti, Fuziwara and Sakezawa[150] (single- and double-welded end fillet lap joints with and without overlap and with 30 or 45° welds, symmetrical double-strap end fillet-welded joint, single glued lap joints, T welded joints, and T welded joints between a web plate and a ribbed flange) and Samsonov[79] (end fillet welds). A study of miniature reproductions of lap fillet joints made of glued celluloid and examined under a polarizing microscope by the Adhesives Research Committee[149] (1926) showed that the maximum stress concentration was found at the ends of the overlap when the joints were loaded in tension. Tests of actual glued joints in wood, however, failed to show that a redesign of the joint with rounded fillets at the ends of the overlap had any effect on strength. The result is not surprising in view of the fact that stress concentration at the ends of side fillet welds or glued joints is an inherent property of the type of load transmittal represented by these joints.

Of these investigators only Coker and coworkers, and Solakian provide quantitative information on maximum stress concentration factors. Coker's models were machined from solid celluloid. Slits were $^1/_{16}$ or $^1/_{20}$ in.

wide and were packed with celluloid strips which lowered the stress in the welds (no quantitative data). The angle of the end fillet was 45° in all cases, but it is stated, again without quantitative data, that the best stress distribution is obtained if the face of the weld is inclined 30° to the shear leg. Coker employed the lateral extensometer method for separating the two principal stresses. Mesmer determined only the boundary stresses and the difference between the principal stresses in the interior.

Solakian used Bakelite models machined from solid plate (unpacked slits 0.015 in. wide) or cemented together as in an actual fillet weld. Results from solid and cemented models were identical. Besides determining the boundary stresses and the difference between the principal stresses in the interior of the various types of welds, Solakian made a complete evaluation of the principal stresses and their normal and shear components for three critical sections of a 45° standard end fillet weld section as shown in Figs. 10 and 11. Whereas in Coker's specimens the straps had about the same thickness as the main plate, each strap in the specimens tested by Solakian was only one-half as thick as the main plate.

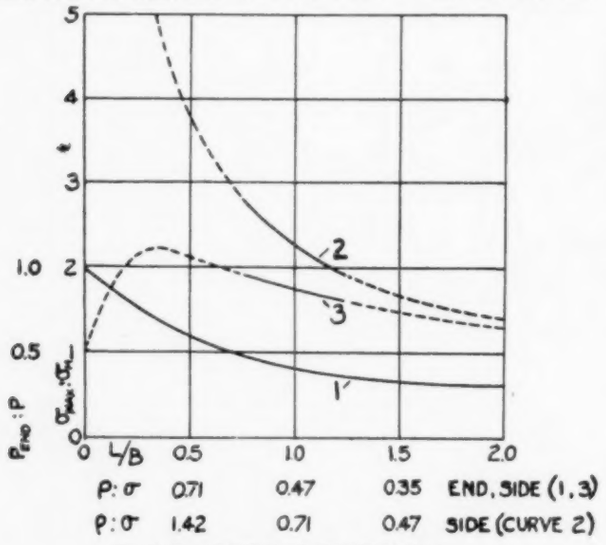

Fig. 8—Stress Distribution in Fillet-Welded Joints

ρ = shear stress in fillet weld
σ = normal stress in strap
σ_m = average normal stress in strap
P = external load on joint
P_{end} = proportion of external load carried by end fillet
L = length of side fillet
B = breadth of strap
Curve 1—P_{end}: P according to Dustin-Rosenthal, box joint
Curve 2—σ_{max}: σ_m side fillet; German Materials Testing Laboratory
Curve 3—σ_{max}: σ_m calculated for box fillet-welded joints[121]

The stress concentration factors determined by these investigators are surprisingly high. In Solakian's studies (double-strap end fillet welds) the maximum stress almost always occurred at the root of the fillet, being from 6.05 (30–60° fillet with volume of weld deposit equal to that in 45° fillet) to 8.10 (45° fillet with underpenetration) times the mean stress in the plate. The stress at the outer end of the shear leg was from 2.12 to 5.75 times the mean stress. Underpenetration (45° end fillet, $1^1/_2$-in. leg, $^1/_4$-in. underpenetration) raised the factor to 8.80, but overpenetration ($^3/_8$ in.) decreased the factor to 4.57, the factor being 6.71 for a weld with equal legs. The root of the fillet was also the source of maximum stress concentration in Coker's specimens of double-strap end fillet welds. Maximum stress concentration factors of over 10 were determined in single-strap end fillet joints, whereas in a joggled joint the factor in the end fillet was less than 2. It should be remembered that the stress concentrations in end fillet joints apply only to narrow straps and do not take account of the variation in stress along the length of the end fillet. The stress concentration factors determined by Solakian by photoelastic tests, Table 4, are no indication of the relative efficiencies of various welded joints in tests to fracture, but are valuable guides for design.

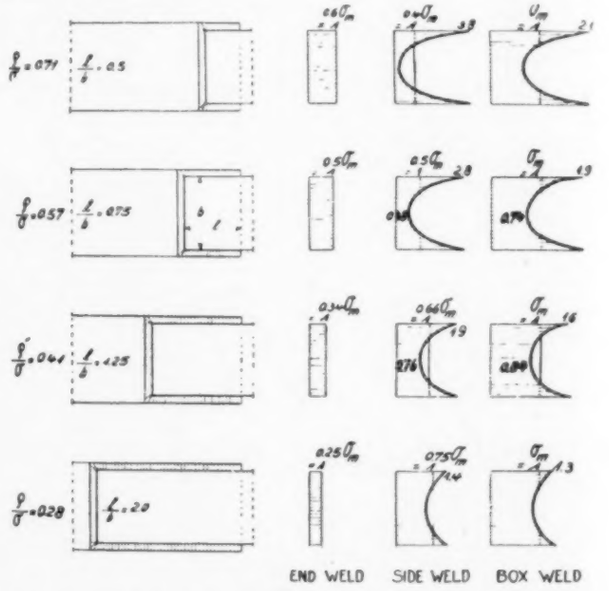

Fig. 9—Stress Distribution in Box Welds at the Cross Section of the Straps at the Inner End of the Side Fillet Welds

ρ = shear stress in fillets
σ = normal stress in strap
σ_m = average normal stress in straps. Bierett[131]

Table 4—Stress Intensities in End Fillet Types *A* to *J*. Solakian[76]

Type of Fillet	Fringe Stress Intensities at Boundaries, Lb./Sq. In. Root of Fillet (pt. *a*)	Neck of Fillet (pt. *b*)	Outer End of Throat (pt. *c*)	Stress Concentration Factors in Terms of Average Plate Stress (pt. *a*)	(pt. *b*)	(pt. *c*)	Description of Fillet
A	4630	3180	850	6.92	4.75	1.27	45° fillet with no penetration of weld
B	3430	2600	575	6.20	4.68	1.04	45° fillet with $^1/_8$-in. penetration of weld
C	4530	3150	505	6.75	4.70	0.75	45° fillet over-size weld
D	5160	2925	1490	7.70	4.36	2.23	45° fillet under-size weld
E	4625	3375	990	6.90	5.05	1.48	45° fillet with $^1/_4$-in. radius undercut at neck
F	4380	1500	955	6.55	2.24	1.42	45° fillet with 1.5 in. radius curve at neck
G	3350	1170	900	6.05	2.12	1.62	30–60° fillet with equal volume of weld metal as in fillet type *A*
H	3670	1780	855	6.60	3.22	1.54	45° fillet *A* with reduced edge (*a*–*d*)
I	4495	3195	685	8.10	5.75	1.23	45° fillet *A* with reduced edge (*a*–*b*)
J	2250	3170	325	4.04	5.72	0.58	45° fillet *A* with no overlap

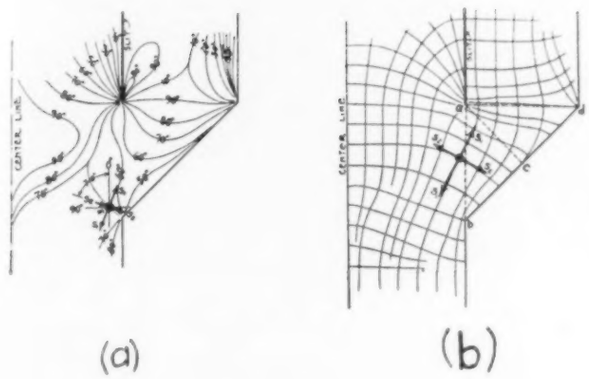

Fig. 10—Principal Stress Distribution in a Symmetrical Flush 45° End Fillet Weld Solakian[78]

(*a*) Isoclinics; along each isoclinic the principal stresses are inclined at a constant angle to the center line
(*b*) Principal Stress Trajectories; the directions of the trajectories at any point are the same as the directions of the principal stresses at that point

The joints tested by Yamanouti and coworkers[150] were prepared from celluloid and joined together by "film cement." The celluloid was $^{5}/_{8}$ inch thick, except for the main plates of the symmetrical double-strap end fillet-welded joint, which were $^{13}/_{16}$ inch thick, the straps being $^{7}/_{16}$ inch thick. The joints were tested in tension (with and without an eccentricity of load of $6^{1}/_{2}°$) as well as in bending. Boundary stresses were determined and principal stress trajectories were plotted; in a few cases (double-strap end fillet weld and glued single overlap joint) isoclinics were also plotted. The trajectories and isoclinics drawn by Yamanouti and coworkers are in poor agreement with the results of Coker and Solakian, who themselves are in good agreement so far as their work overlaps. The maximum stress concentration factors determined by the Japanese investigators are surprisingly low, rarely above 1.7, and for this reason are not reproduced in this review.

Besides the numerical evaluation of stress concentration factors, the photoelastic studies have shown the nature of the elastic stress distribution in fillet welds. The diagrams of lines of principal stress and isoclinics drawn by almost all investigators show, in general, that the inclination of either principal stress is not constant across the critical section (throat) or legs of the fillet weld. The limitations of the approximate methods for computing fillet welds are strikingly shown by the photoelastic results.

Rubber Model Method

Models made of thin soft rubber sheet ruled with an orthogonal net are often used to study stress and strain in two dimensional stress problems. Upon being stretched from 50 to 100% the model reveals the characteristics of strain, but the stress concentration factors deduced therefrom are much less than those evaluated from steel models. Poisson's ratio and the elastic moduli of ordinary grades of rubber are not constant. Furthermore, the shape of the heavily strained rubber model is usually far different from the unstrained.

Nothing of a quantitative nature and little of a qualitative nature that is not misleading may therefore be expected of rubber or lead models of fillet welds. The ease with which the models may be prepared probably accounts for their comparatively extensive use. In 1926 Sonderegger[80] showed the stress distribution in Höhn straps (reinforcing strap with box fillets) by means of a rubber model. Vogel[148] studied rubber models of symmetrical and unsymmetrical side and box fillets. The British Engine Boiler and Electrical Insurance Company,[51] Griffin,[16] Vivian[82] and Kinkead[83] employed rubber models of end fillets. Schaechterle[84] deduced principal stress trajectories for side fillet welds from

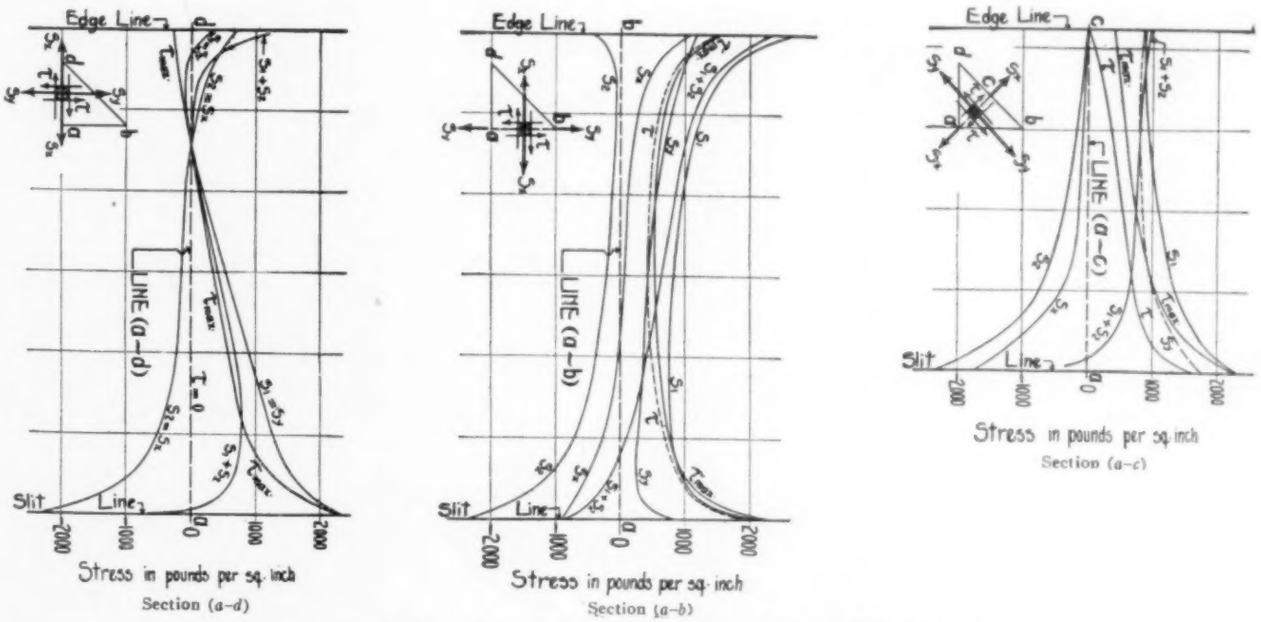

Fig. 11—Distribution of Stresses Along Three Sections of a Flush 45° End Fillet Weld, Solakian[78]

S_1 and S_2 are the principal stresses
$T_{max.}$ is the maximum shear stress

rubber models, and Graf[85] found from a rubber model that the gap between plate and strap is a maximum midway between the side fillet welds. Schick[86] studied models of side fillets, inclined fillets and side fillets reinforced with plug welds.

Brittle Coating or Maybach Method

A rapid method of determining the regions of maximum tensile strain and consequently the direction of the maximum principal normal stress is that employing a brittle coating, such as whitewash or special varnish. The coating must have good holding power and low elongation at fracture so that cracks will occur before the proportional limit of the underlying wood or steel has been reached. If the magnitude as well as the direction of the principal stresses is desired the coating method must be supplemented by strain measurements.

Little use has been made of the method in the study of fillet welds. Shimkin[87] observed the patterns in mill scale on distorted end and side fillet welds and noted an area beyond the straps in fractured side fillet welds that appeared to have undergone no strain. Kayser and Herzog[88] used the brittle coating method employing a thin varnish to show the lines of principal stress in side and end fillet welds in mild steel with and without rivets. Stress concentration factors of 2 to 3 were computed for side fillet welds in compression and a singular point in the isoclinics was observed in the straps of side fillet welds if the ratio of width to length of strap were large.

Fatigue Tests

Points of maximum stress concentration in fillet welds are sensitively detected by fatigue tests, as explained in detail in Fatigue Strength of Welded Joints. The fatigue results are affected to a great extent by welding defects which have served to mask the effect of the geometrical shape of the joint. Erlinghagen's[89] extremely low pulsating tension fatigue limit of 6000 psi for arc-welded side fillets in mild steel (static strength of side fillet joints of same dimensions was 53,000 psi) is hardly explained by defects, however, because the straps themselves failed at the ends of the side fillet welds. Thus, tension fatigue fractures of end fillets always commence at the toe rather than at the root of the fillet, which suggests that extremely local and high stresses are caused by undercut or other defects. In side fillets tension fatigue fracture usually occurs at the inner end of the fillet, which is in agreement with several of the theoretical investigations.

Other Methods

The Fry etching reagent has been applied to sections of end fillet welds in mild steel by Füchsel[90] and Kochendörffer,[91] but the results are of no importance. As the latter found, the weld metal itself does not respond properly to the Fry reagent and the operation of welding causes serious structural changes in the vicinity of the weld which obscure the effects caused by local permanent deformation.

Glued paper models of side and box fillet welds have been studied by Hertwig,[92] who concluded that fillet welds can never be expected to provide so favorable stress distribution as riveted joints. Hertwig's observations were entirely qualitative.

Isaacs[93] used three methods to determine the stress distribution in side fillet joints by analogy but his descriptions of experimental arrangements are sketchy. He was apparently successful with a net of cotton threads and with a rubber diaphragm, but was unsuccessful with a soap film apparatus. He employed distortion measurements obtained by the first two methods to calculate ratios of maximum and minimum shear stress along the weld to average shear stress. Ratios so determined were much nearer unity than similar ratios calculated by the cosh formulas.

Painstaking observations of distortion in end fillet welds in mild steel led Bibber[10] to the conclusion that at the instant the weld is developing its rated strength, that is, at the time of fracture, the stress distribution across leg and throat is practically uniform, the lines of stress being parallel to the contour of the weld.

A hydraulic analogy (stream line flow) was used by Kochendörffer[91] to show the advantages of smooth transitions in end fillets, and Geldbach[94] used a similar hydraulic method to demonstrate the more favorable stress distribution in T joints obtained with concave, as compared with convex, fillets. The methods yielded only qualitative results.

The Theory of Elasticity Applied to Fillet-Welded Joints

The fillet weld is an important example of a body stressed predominantly in shear. Those who have attempted to determine the stress distribution in fillet welds with the aid of the theory of elasticity have found that the problem is complicated by an excessive number of variables, and by the difficulty of obtaining unambiguous experimental information. Even so, the theoretical investigators have secured considerable insight into the large-scale stress distribution in end fillets. In other words, the stress distribution along the length of the side fillet is known with some certainty as well as the stress distribution in the cross section of the end fillet. All in all, the theoretical development of the side fillet is more advanced than that of the end fillet, although, curiously enough, more experimental information has been collected for end, than for side fillets. It is universally realized that theoretical stress distributions are changed by local inelastic action at points of maximum stress concentration. The change is favorable, but may not apply for welds under some conditions of fatigue, as Priest[2] pointed out.

Side Fillet-Welded Joints

The side fillet joint has provoked a large number of theoretical investigations. Nevertheless the viewpoint and results of any one investigator cannot at present be expressed without reservation on account of the absence of thorough experimental investigation. The history of the problem of the stress distribution in side fillet welds appears to consist of two phases: (1) in which the fillet is assumed to act as an idealized shear surface of relatively large depth; (2) in which the characteristics of deformation of an actual triangular fillet are dealt with. The equations developed by the different investigators for the large scale distribution of shear stress are generally in good agreement. The small scale stress distribution in welds of the customary shape remains a matter for further investigation.

Cosh Equations.—The outstanding feature of the stress distribution calculated by almost all investigators is that it obeys a law involving hyperbolic functions. The essential features of a stress distribution of this type were determined in 1910 by Arnovlević,[95] who analyzed the idealized joints shown in Fig. 12. He assumed that the shear force acting at any point on the area of contact of the two concentric bars of different materials is proportional to the relative displacement, which, in turn, is determined by the normal stress in the two bars.

He also presupposed that only shear forces (he had friction or clamping forces in mind) acted on the surface of contact and that effects due to Poisson's ratio could be neglected. A simple analysis led to the expression

$$\frac{d^2S}{dx^2} = A^2S - B$$

where S is the average normal stress in the inner bar and A and B are constants. The general integral of this expression is $S = C_1 e^{Ax} + C_2 e^{-Ax} + B/A^2$ which, on simplification, becomes

$$S = 2C \cosh Ax + \frac{B}{A^2}$$

An expression for the shear stress is obtained by the assumption that shear stress is proportional to relative displacement. It is thus shown that, for bars with cross-sectional areas inversely proportional to their respective Young's moduli, the curve of shear stress *vs.* distance along joint is symmetrical with respect to stress about $x = \frac{L}{2}$ (Fig. 12), the minimum occurring at this point.

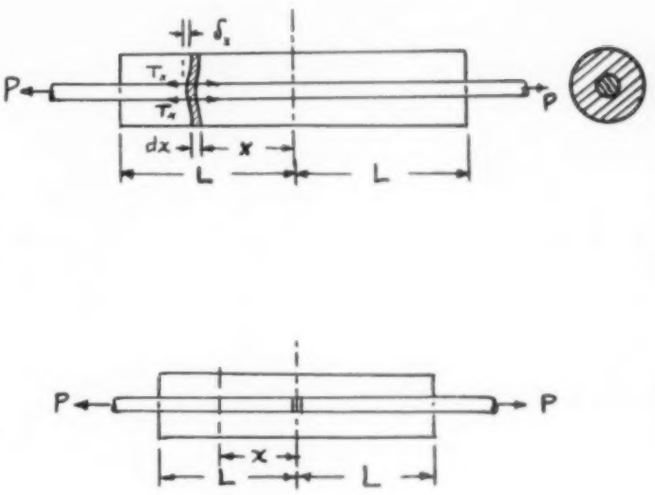

Fig. 12—Concentric Bar Problem Simulating a Side Fillet-Welded Joint. Arnovlevic[95] Upper Diagram. Inner Bar Continuous. Lower Diagram. Inner Bar Discontinuous

The application of analytical methods akin to those of Arnovlević to side fillet-welded joints was made in 1928 by Troelsch.[96] The uniting prisms of weld metal were assumed to be rectangular in cross section, and the stress distribution on any cross section of strap and main plate was assumed to be uniform. Troelsch assumed that the shear stress per unit length of weld is

$$V = qD$$

where q is the shearing deformation of the weld at any section along the length of the weld, and D is a proportionality constant, called the Detrusion Ratio. On these bases he developed an equation for V involving hyperbolic functions—an equation of the so-called Cosh type.

Upon being applied to numerical examples, the equation predicted maximum shear stresses at the ends of the weld of 3 to 6 times the average shear stress. The equation was criticized[97] because the area of weld metal in contact with the main plate, as well as the length of the fillet, had no effect on the stress distribution. It was also pointed out that the assumption of uniform stress distribution in strap and plate was directly contrary to experimental evidence, and might lead to deviations as high as 30%.

The main difficulty with Troelsch's investigation was the detrusion ratio, which required experimental determination and depended on the cross-sectional dimension of the welds and on the material. Exactly what shearing deformation was to be measured in order to determine D was not known.

The problem of non-uniformity of stress distribution across a transverse section through straps and plate was solved by Weiskopf and Male,[53] who showed that the center line of the straps remains a straight line after deformation. The analysis proceeded from the reasonable assumption that the strap is a rectangular body acted upon by external shear at its sides. Direct stresses due to bending moments are neglected. The expression derived for deformation at the weld involves hyperbolic functions. This analysis permitted the determination of the extent to which the area of contact of weld with strap was effective in transmitting stress.

In addition to the determination of these "effective" areas, Weiskopf and Male attacked the problem of the internal behavior of a weld of triangular cross section when subjected to shearing forces on the two legs. They assumed that the deformation is zero at the root and maximum at the hypotenuse. That is, the cross section is considered pivoted at the root, the shear force being uniformly distributed across the critical section or throat. The equation ultimately arrived at for the shear load per unit length at any point along the length of the side fillet is

$$V = \frac{Pr}{bN(r+1)} \left\{ \frac{1 + r \cosh \frac{Z}{b}}{r \sinh \frac{Z}{b}} \cosh \frac{x}{b} - \sinh \frac{x}{b} \right\}$$

where P = total external load

r = ratio of effective cross sections of strap to plate $= \frac{a_1}{a_2}$

$b = \left[\frac{a_1 a_2 E}{(a_1 + a_2) N D} \right]^{\frac{1}{2}}$

E = Young's modulus

N = number of welds in the cross section of the joint

D = detrusion ratio of the fillet $= \frac{3E}{10}$ for an isosceles right triangular fillet

Z = length of a weld

z = distance from outer end of weld to the point under consideration.

The distribution of shear stress is thus independent of the cross-sectional area of the fillet, bearing in mind the limitations set by a_1 and a_2.

The stress distribution predicted by the equation is in close agreement with the experimental results of Smith,[84] Male[53] and others, provided the detrusion ratio experimentally determined is corrected for failure of the extensometer points to make contact with the apexes of

the weld. The theoretical value of the detrusion ratio for a right angle fillet is $D = \frac{2}{3} G \frac{T}{H}$

where G = modulus of elasticity in shear

T = length of throat of weld

H = length of hypotenuse of weld.

As before, the assumption for determining D is that the distribution of shear stress on the legs of the weld is uniform.

The approach to the side fillet problem through the detrusion ratio has also been made by Fillunger,[98] MacKay and Bain,[56] Hovgaard,[99] Kalina,[100] Callandreau,[101] Bleich[102] and Neuber.[103] Each arrived at an equation for shear stress distribution of the cosh type. MacKay and Bain found that a good value of the detrusion ratio was $0.43E$, where E is Young's modulus.

Like Weiskopf and Male, and MacKay and Bain, Hovgaard verified his cosh equation by strain gage (Berry) measurement on welded joints (each weld was 30 in. long). He found that the detrusion ratio could be replaced by a displacement coefficient which he defined as the factor by which the shear stress (not load) in the cross section of a weld at any point, must be multiplied to yield the average linear displacement of a section of the strap relative to the corresponding section of the plate at the point in question. His formulas thus involve the cross-sectional area of the weld, which is also true of the formulas of Bleich,[102] Loschkin,[99] Callandreau[101] and Neuber.[103] The theoretical constancy of the displacement coefficient (Neuber calls it the shear modulus of the weld) is not affected, however. Hovgaard found by experiment that the value of the displacement coefficient for mild steel welds was 0.31×10^{-7}.

Bleich[102] prepared Table 5 which gives the value of the stress concentration factor U_x along a side fillet weld for common values of L/A

Table 5—Stress Concentration Factor for Single-Lap Side Fillets. Bleich[102]

L/A	$X/L = 0$	0.1	0.2	0.3	0.4	0.5
20	2.03	1.41	1.00	0.75	0.61	0.57
30	2.42	1.52	0.98	0.66	0.50	0.45
40	2.72	1.61	0.95	0.57	0.41	0.35

where A = throat of weld

L = length of weld

$U_x = \frac{T_x L}{P}$ = stress concentration factor

T_x = shear load per unit length in weld at point

P = load on joint

X = distance along weld from either end, the stress distribution being symmetrical about the midpoint of the length, if the plates are of equal thickness.

Kalina,[100] who has used his cosh equation to show the stress peaks at the ends of each bead in interrupted fillets in welded plate girders, assumed a uniform distribution of shear stress over the throat of the weld, and that the stress problem was consequently uni-axial. Curves showing the application of his results to a single-lap side fillet are given in Fig 13. Since the bar sections are unequal the minimum of the shear curve is displaced toward the larger bar, the curve remaining symmetrical with respect to stress. Kalina's equations are of the same form as Fillunger's.[98]

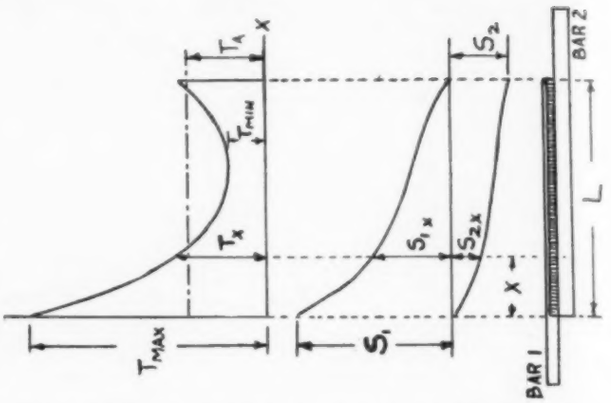

Fig. 13—Calculated Stress Distribution in a Single Lap Side Fillet-Welded Joint

T = Shear stress
T_A = Average shear stress
S_1 = Normal stress in bar 1
S_2 = Normal stress in bar 2

Ratio of cross-sectional areas of bars 1 and 2 not given but if cross sections are equal the shear stress distribution curve is symmetrical about the middle of the length of the weld. Detrusion Ratio = 8.5 to 11 $\times$ 10^6 psi. Kalina[100]

The strain-energy theory and the principle of least work were the basis of the cosh theory developed by Callandreau.[101] He gives quantitative expression to Höhn's qualitative statement that at a critical point along the side fillet the relative internal displacement in the weld is zero. In other words, at the distance x given by

$$\lambda \cosh r(L-x) - \cosh rx = o$$

the normal stresses in the two plates of a single-lap fillet are equal in absolute value.

$$\lambda = \frac{S_1}{S_2}$$

S_1 and S_2 = cross-sectional area of the plates

$$r = \left(\frac{G}{E} S \frac{S_1 + S_2}{S_1 \cdot S_2} \right)^{\frac{1}{2}}$$

S = total cross section of welds

G = shear modulus of elasticity

E = Young's modulus

L = length of weld

x = distance along weld from the end of the plate.

Neuber[103] also studied the single-lap side fillet and developed a cosh equation, without however detailing his procedure. The constant involved in his formula he refers to as the shear modulus of the weld to which he assigns the value $0.25\,E$ in order to secure agreement with Hoeffgen's[65] results. He also shows that the displacement coefficient in the equations of Hovgaard, Fillunger and others, whose equations involved the dimensions of the weld cross section, is not a material constant, but increases as the dimensions of the weld increase.

Rivet Formulas.—Another approach to the problem of stress distribution in side fillets has been through the formulas for stress distribution in riveted joints. Thus,

Batho[104] assumed that the four welds of a double-strap side fillet joint may be regarded as equivalent to four rivets in a row in a riveted joint, and arrived at a cosh equation similar to those appropriate for riveted joints. The stress distribution along a single-lap joint is identical with that along a double-strap joint except that the absolute value of the stresses is twice as great. His formula shows that the stress concentration factor decreases as the length of the weld increased.

Petermann[105] also extended his formulas for load distribution in a riveted joint to a joint with continuous rivets, which he assumed represented the side fillet-welded joint. The basic assumption, as in the equations for riveted joints, is that the stress in finite sections of the joint is constant. The equation he developed is of the familiar cosh type. His formula shows that the stress concentration factor, Table 6, increases as the length of the weld is increased. The difficulty with extending rivet formulas to side fillet-welded joints is that there is no justification for assuming that a weld is the limiting case of closely spaced rivets, as Dernedde pointed out.

Table 6—Stress Concentration Factors in Side Fillet-Welded Joints. Petermann[105]

Length of Weld	Stress at End of Weld/Stress at Middle of Weld
4 × breadth of strap	1.86
6 × " " "	3.84
8 × " " "	5.37

Other Cosh Equations.—Equations of the cosh type have been developed by Pilgram[106] for single-lap side fillet joints (two welds of equal or unequal length). In addition to presupposing uniform distribution in the plates, he assumed that one of the plates was rigid and did not share in the deformation of the weld. As a result, the stress at one end of the weld became infinite. In his later paper he modified his procedure (differential equations of equilibrium satisfied by appropriate boundary conditions) to secure a symmetrical distribution of shear stress and normal stress at the edges of the stress. But Kohl's criticism of Pilgram's highly artificial, though severe, assumption of ridity still appears just.

Two abstract plane-stress problems solved by Melan[107] may also be classed with those already dealt with. In the first problem Melan found by means of the stress function, that an equation of the cosh type specified the stress distribution along the edge of a semi-infinite plate of finite thickness, H, to whose edge was welded an axially loaded rod of finite cross section F. The character of the stress distribution is shown in Table 7, the stress becoming negligible at points in the plate sufficiently remote from the weld. The results show that the shear stress is maximum at the end of the weld.

Table 7—Stress Concentration Factors for Single-Lap Side Fillets. Melan[107]

$\frac{Hx}{2F}$	$T\frac{F}{P}$	$S_z\frac{F}{P}$
0	−0.0	−0.5000
0.2	−0.2061	−0.3617
0.5	−0.1070	−0.2740
1.0	−0.0547	−0.1981
2.0	−0.0230	−0.1270
6.0	−0.0039	−0.0507
12.0	−0.0008	−0.0269

x = distance along the edge of the plate measured from the unloaded end of the rod (strap)
T = shear stress in weld (shape of weld not specified)
S_z = normal stress parallel to the axis of the strap and to the edge of the plate
P = the axial load.

The second problem dealt with by Melan was the stress distribution in the vicinity of a bar welded to the surface of an infinite plate. Equations for the stress distribution are of the same type as for the first problem.

Dobronetsky and Diatlov[108] have applied a cosh equation developed by Smit and Danilov (the work of Smit and Danilov was not available to the reviewers but the equation is of the familiar cosh type) to an ingenious system of side fillet welding in which the ends of the fillets, being the regions of maximum stress concentration are strengthened locally by additional beads of weld metal. Although Dobronetsky and Diatlov calculate the combination of two sizes of welds necessary to replace a bead of uniform size, they do not appear to have calculated the diminution in maximum stress concentration to be expected theoretically.

The last of the cosh equations to be considered from the standpoint of welded joints is a modification of Troelsch's equation used by Werner.[109] Using Troelsch's assumptions, Werner showed that the distribution of shear stress along the welds of a single lap side fillet joint is uniform if both plates taper from full plate thickness at the beginning of the weld to nil thickness at the end of the weld. If the thickness at the end of the taper is only $^1/_5$ the total plate thickness, the maximum stress concentration factor is still 2.5.

In concluding the discussion of cosh equations it may be said that they closely fit the trend of the experimental results. The neglected factors therefore do not radically change the stress distribution in the welds with the dimensions tested.

Other Theoretical Investigations.—One of the first theoretical attempts to map the stress distribution in symmetrical double-strap side fillet-welded joints was made by Rosenthal.[52] He assumed that the shear strain d_s at a point in the weld is directly proportional to the normal stress S_s at that point

$$S_s = Ed_s = E(ux + v)$$

where E = Young's modulus

x = distance measured from center of the length of the weld in either direction,

and u and v are constants. He showed that the shear strain in the straps along the welds at a point x is $d_{xs} = \frac{P}{2EA_s}\left(1 + \frac{2x}{L}\right)$ and the corresponding strain in the plates was

$$d_{xp} = \frac{P}{2EA_p}\left(1 - \frac{2x}{L}\right)$$

where L = length of one weld
and A_s and A_p are the total cross-sectional areas of straps and plate, respectively, P being the external load applied to the joint. The relative displacement then became

$$\Delta_s = \frac{2m + 1}{2m\,E}\left(\frac{1}{A_s} + \frac{1}{A_p}\right)\frac{P}{L}\frac{B^2}{4}$$

where m = Poisson's ratio
and B = breadth of one strap. Although the basic assumption of Rosenthal's theoretical analysis appears artificial, his equations are in fair agreement with his experimental measurements (see section on Experiments). Rosenthal[110] also believed that the stress distribution in fillet welds is the same in tension as in compression. Kist[111] is of the opposite belief and is supported by the results of fatigue tests by Ros[112] which showed that fatigue failure in compression originated at the root rather than at the toe of the fillet.

Goelzer[113] and Gerbeaux[114] have worked out the stress distribution in a single side fillet weld of finite length assuming that the applied load acts along one shear face of the weld. Goelzer, who originated the idea, also assumed that the shear stress in the cross section of the weld is maximum along the throat. He then obtained an expression for shear stress on the throat depending on the angle of the weld and its length. This equation is of the same type as for the corresponding problem in end fillets (see section on End Fillets) and indicates that for 45° welds, the side fillet is 50% more resilient than the end fillet.

Gerbeaux, who confines his attention to a single side fillet in which the legs are at right angles, adopts Goelzer's assumption that the shear stress is maximum on the throat section. He then assumes that the distribution of shear in the plane of the throat at a point along the length of each leg of the weld obeys a law of the exponential type $T = Ke^{z}$; where K is a constant and z is the distance of the point from the end of the weld. The assumption is justified by Hoeffgen's[65] experimental results, according to Gerbeaux, but it is the reviewers' belief that, beyond experimental confirmation, the assumption is without sound basis. The analysis of Goelzer and Gerbeaux is hardly applicable to side fillet welds in practice because the applied load never acts along one leg of the weld. The results of Goelzer, modified by Kayser,[25] are shown in Fig 14. The theoretical curve for shear stress shows that the shear stress at the ends of the welds is zero, the peak or contract stresses being localized close to the ends.

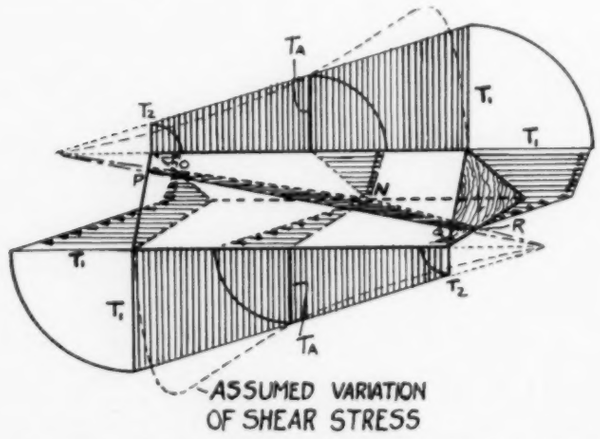

Fig. 14—Schematic Diagram Illustrating the Distribution of Shear Stress T in a Side Fillet Weld

T_A = average shear stress
Surface *NOPQR* is the surface of zero shear. Kayser[25]

An instructive discussion of the difficulties encountered in the analysis of side fillets is given by Kohl,[115] whose results, based as they are on uniform stress distribution in the plate at the end of the welds, are, however, unsatisfactory. According to Kohl, the main difficulties are: (1) the strain along the welds depends upon the distortion of the straps and plate, which, in turn, depends on the geometrical shape of the joint and character of loading; (2) the distribution of shear stress along the side fillet weld is statically indeterminate. His results support the contention that Cajar's[116] assumption that shear stress in the weld is directly proportional to longitudinal strain is incorrect. An unusual form of stress distribution along side fillets has been suggested also by Vandeperre,[117] without adequate theoretical support.

An attempt to determine the stress distribution in the cross section of a side fillet weld has been made by Gardena,[118] Fig 15. The side fillet weld is considered as a beam, the triangular area *OBD* being neglected. The shear load per unit length in an infinitesimal strip dy is $T_y = G\frac{N_x}{Z} = G\frac{N_x \sin a}{(h-y)}$

where G = shear modulus of elasticity

N_x = relative displacement

$$Z = \frac{h-y}{\sin a}$$

The total load carried by the weld cross section is

$$P = \int_o^{h-h_o} T_y \cos a \, dy = GN_x \sin a \cos a \, ln \frac{h}{h_o}$$

whence $T_y = \dfrac{P}{ln \dfrac{h}{h_o}(h-y)\cos a}$; $h_o = o$, $Ty = \infty$

The result that the shear stress is infinite at the root of the weld is not new and indicates that the assumption of beam conditions is not valid. Strain measurements on a large model set up by Gardena in a special measuring frame were said, nevertheless, to confirm the final equation.

Chassaing[119] also thought that the side fillet could be considered to act as a beam but in the sense that the bending couple due to the eccentricity of the straps was applied near the ends of the entire length of weld. He was thus led to the result that the maximum shearing stress occurred at the center of the length of the weld, the shear stress at the ends being zero. Unfortunately, Chassaing made no attempt to include other more obvious factors than the bending effect, which has been neglected by most investigators. It is not surprising therefore, as Troelsch[97] pointed out, that his equation does not agree qualitatively with experimental results.

The Side Fillet Weld in General.—Although the object of the present review is only the stress distribution in fillet-welded joints, two theoretical investigations of the side fillet weld in general have been made. In 1928, Gillespie and coworkers[120] considered a side fillet weld to be equivalent to a thin web joining the two members shown in Fig 16. The arrangement of webs and bars in Gillespie's analogy permits the application of Filon's[121] equation (of the cosh type) for shear due to nearly opposite loads, the webs being considered as beams of great depth compared with the distance between loads. The characteristic of Filon's equation is that for the case of Gillespie's webs the parabolic distribution of shear characteristic of Saint Venant's type of flexure does not begin to be approximated until the ratio of distance between the nearly opposite loads to the depth of the web or beam is greater than unity. For values of this ratio less than unity—the case of side fillet welds under Gillespie's assumption—the stress ditribution consists of two peaks near the point of application of the load with a minimum at the center of the web, Fig 17. The stress distribution represented by the two peaks is closely similar to that obtained by combining two contact stresses, one on each face of the beam.

The Filon equation has been confirmed by photoelastic investigation (Coker[122]) of the center line of the web in Gillespie's case. Coker, however, found preponderantly high stress concentrations at the corners of the web. He also found that the lines of principal stress crossed at 45° all along the center line of the web except

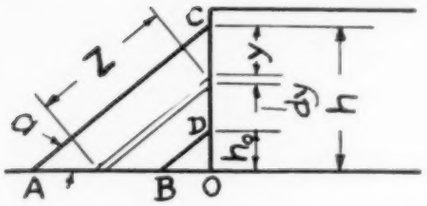

Fig. 15—Cross Section of a Side Fillet Weld. Gardena[118]
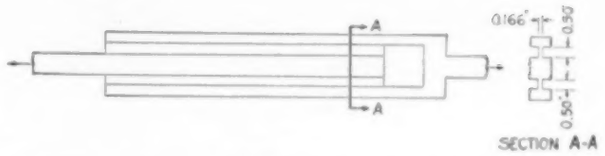
Fig. 16—Analogy of a Side Fillet-Welded Joint Tested by Gillespie and Coworkers[130]

Mild Steel
Length of Web = 13 inches
Cross sectional area of inside member = 0.95 sq. in.
Cross-sectional area of each outside member = 0.475 sq. in.

at a short distance ($^1/_4$ in. in his celluloid model) from the ends. In other words, except at its extreme ends the web is subjected to shear stress. Coker did not evaluate the magnitude of the high stress concentrations at the corners of the web.

The Filon equation was also confirmed by Gillespie's extensometer measurements on his steel "web" specimens. The maximum deviation was 20%. Gillespie and coworkers, however, did not consider that the shear stress at the ends of the webs (free boundaries) must be zero.

The general case of the side fillet weld in connecting a reinforcing strap to a solid plate has been exhaustively investigated theoretically and experimentally by Hovgaard and coworkers.[123] As in the work on side fillet joints by Hovgaard quoted earlier in this section, the assumption of a displacement coefficient of average or constant value is made. The experimental results were not consistent and did not appear to justify the assumption.

A slotted plate with a web welded in by means of continuous side fillets was also studied. The expression for the shearing stress T_s in the weld at any point x was found to be of the form

$$T_s = K\frac{x}{\left(\frac{L^2}{4} - x^2\right)^{\frac{1}{2}}}$$

where L = length of slot and inserted web

x = distance to point on weld measured from the center of the length of the weld, and

K is a coefficient that is constant along the length of a given combination of web and plate, but varies with dimensions and elastic properties. The experimental results again completely failed to indicate a constant displacement coefficient.

Ohno[124] applied Troelsch's formula to the stress distribution in a plate reinforced by a strap attached by side fillet welds and, of course, arrived at an equation of the familiar cosh form.

End Fillet-Welded Joints

Comparatively few attempts have been made to determine the theoretical stress distribution in end fillet-welded joints, perhaps because the problem, according to Hovgaard, is more difficult or less important than the corresponding problem for side fillets. In general, the problem of the end fillet of any shape consists first in obtaining expressions for the principal normal and shear stresses at all points in a cross section of the weld and vicinity parallel to the applied load, and then of formulating the change in stress distribution along the length of the weld. Practically no progress appears to have been made in solving the second part of the general problem.

About 1925 the German National Materials Testing Laboratory[24] (Neese) deduced the stress distribution along the tension and shear faces of symmetrical double-strap, end fillet joints (leg of weld = breadth of strap) as a plane problem in bending. The bending stress on the tension face varied linearly from O at the toe to a maximum at the root, the resultant $\frac{P}{2D^2} + \frac{3P}{D^2}(^1/_2 - \frac{E_1}{D})$ (where P = applied load, E_1 = moment arm of resultant about root = $\frac{D}{3}$ and D = breadth of strap and leg of weld) acting at a distance of $^1/_3$ the thickness of the strap from the plate. On the shear face the shear was assumed uniformly distributed, but the normal stress (maximum bending stress = $3PE_1/D^2$) varied from maximum compression at root to maximum tension at toe. A study of stress distribution along the weld was also made but the results are inadequately explained by Neese. A concentration factor of 8.9 for shear stress at the root of the weld was arrived at.

Goelzer[113] attempted to generalize the equations for maximum shear (T) and normal stress (N) and stress distribution in single-lap end fillets to include welds of any triangular cross section. Adopting the procedure for a problem in plane stress he developed the following equations as well as an equation for computing isoclinics.

$$N = \frac{F}{2a\,m^2}\left[m^2 - 3 + (m^4 + 10m^2 + 9)^{\frac{1}{2}}\right]$$

$$T = \frac{F}{2a\,m^2}\left[m^4 + 10m^2 + 9\right]^{\frac{1}{2}}$$

where a = thickness of strap

F = external load per unit length of fillet assumed (1) to act normal to and at the center of the tension face, (2) to be distributed uniformly along the length of the fillet, and (3) to exert no bending moments.

m = tangent of the toe angle of the weld at the tension face.

The formulas presuppose that the shear stress on the shear face increases linearly with the distance outward along the shear face. Less plausible results were obtained if the shear stress was assumed constant or to vary with the square of the distance. His results are summarized in Table 8 which shows the stress concentration factors for normal (K_1) and shear stress (K_2) as a function of angularity of the weld. For a 45° fillet the stress concentration is only a little over 2, which seems small. Goelzer fully discusses the limitations of his formulas.

A less general consideration of the single-lap end fillet, Fig. 18, with 90° root angle was made by Streletzky and Nikolaieff,[50] who also assumed that the stress distribution along the length of the weld was constant. Starting with the differential equations of elasticity the authors

Table 8—Stress Concentration Factors for the Root of Single-Lap End Fillet Welds. Goelzer[113]

M = tan (Toe Angle at Tension Face)	K_1 (Normal Stress)	K_2 (Shear Stress)
0.50	1.295	6.795
1.00	1.235	2.236
1.50	1.178	1.345
2.00	1.135	1.01

For the toe on the shear face the factor K_1 is 6 when $m = 1$ and $K_1 = 4.17$ when $m = 2$.

quickly obtain the following formulas for the principal stresses.

$$N_1 = \frac{S_{xx} + S_{yy}}{2} + {}^1/_2\left[(S_{xx} - S_{yy})^2 + 4\,T_{xy}{}^2\right]^{\frac{1}{2}}$$

$$N_2 = \frac{S_{xx} + S_{yy}}{2} - {}^1/_2\left[(S_{xx} - S_{yy})^2 + 4\,T_{xy}{}^2\right]^{\frac{1}{2}}$$

The maximum shear stress is

$$T_{max.} = {}^1/_2\left[(S_{xx} - S_{yy})^2 + 4\,T_{xy}{}^2\right]^{\frac{1}{2}}$$

where S_{xx} = normal stress parallel to X axis

S_{yy} = normal stress parallel to Y axis

T_{xy} = shear stress.

The derivation is such that normal and shear stresses in the weld are a function only of the angle determining any radius vector from point 0. The stresses are thus constant along the length of a radius vector but are different for different radii vector. Therefore, the isostatics are not curved. Numerical results for a 45° weld are shown in Fig 19. The stress distribution is more favorable for $F = 60°$ than for $F = 45°$ or 30°.

A comparison of these results with results obtained by considering the end fillet weld as a fixed cantilever beam is given in Table 9, where $S_{yy} = 3N\,(H/A)^2$, N being the stress uniformly distributed over the upper plate and H and A being the length of the leg of the weld on tension and shear faces, respectively.

Table 9—Stress Concentration Factors in Single-Lap End Fillet Welds. Streletzky and Nikolaieff[50]

Angle F, Degrees	Exact Theory S_{yy} Max.	Exact Theory S_{yy} Min.	Approximate (Beam) Theory S_{yy} Max.	Approximate (Beam) Theory S_{yy} Min.
60	0.632	−1.529	1	−1
45	2.330	−3.660	3	−3
30	8.050	−9.752	9	−9

In view of the fundamental difference in the assumptions involved in the two theories, the results are not greatly different.

Schaechterle[84] also based his calculation of end fillets on the beam principle. Since he assumed that the resultant acted at $^1/_4$ the thickness of the strap from the plate, the stress concentration factor at the root of a 45° weld turned out to be 2.5 instead of 3.0.

A good criticism of the conceptions of stresses in end fillets held by his predecessors is given by Kochendörffer,[45] who believes that the normal stress on the shear face increases linearly from zero at the root to a maximum at the toe. He also shows, using d'Alembert's principle, that the resultant on the tension face is inclined to the axis of applied load. However, he does not provide

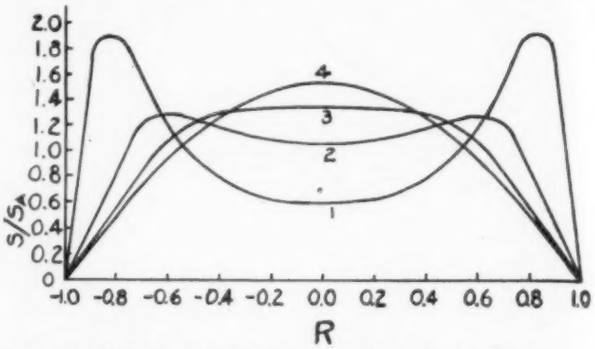

Fig. 17—The Distribution of Shear Due to Nearly Opposite Loads. Filon[121]

S = Shear stress at a given point
S_A = Average shear stress on the cross section
R = Ratio between distance from the neutral axis to the given point and the distance B from the neutral axis to either extreme fiber

Curve 1 $L/B = \pi/19 = 0.1745$
Curve 2 $L/B = \pi/9 = 0.3491$
Curve 3 $L/B = \pi/6 = 0.5236$
Curve 4 $L/B = \pi/3 = 1.0472$ (almost the same as L/B = infinity)

L = Distance from either load to the section under consideration, which is midway between the two nearly opposite loads

equations for determining the change in angle of the resultant with distance along joint, nor for calculating the stress distribution.

An approach to the double strap end fillet problem, Fig. 20, by means of the Airy stress function has been made by Olsson.[125] If S_o is the stress assumed uniformly distributed in each strap, the equations for S_x, S_y and T are

$$S_x = S_o + \frac{2S_o}{3A}\left(y - \frac{A}{2}\right)$$

$$S_y = S_o + \frac{2S_o}{3A}\left(x - \frac{A}{2}\right)$$

$$T = -\frac{11}{12}\,S_o - \frac{S_o}{3A^2}\left(x - \frac{A}{2}\right)\left(3x - \frac{A}{2}\right)$$

The distribution of these stresses is shown in Fig. 21. The equations for the principal stresses are

$$S_1 = -2DA\left[\left(x-\frac{A}{2}\right)+\left(y-\frac{A}{2}\right)+\frac{3}{2A}\left\{\left(x-\frac{A}{2}\right)+\left(y-\frac{A}{2}\right)\right\}^2\right]$$

$$S_2 = 2S_o + D\left[3\left\{\left(x-\frac{A}{2}\right)^2+\left(y-\frac{A}{2}\right)^2\right\}-A^2\right]$$

where

$$D = \frac{(1-u)S_o}{2A^2\left\{\frac{2}{3}\,(1-u) + \frac{109}{240}\,(1+u)\right\}}$$

u = Poisson's ratio

The distribution of principal stresses is shown in Fig. 22. Lines of constant principal stress (isostatics) are parallel (S_1) and perpendicular (S_2) to the hypotenuse of the weld. Equations for strain are also developed on the basis of the stress function, which, of course, is employed on the assumption that the weld is of infinite length (no variation of stress distribution along the length of the weld). The remarkably poor agreement of Olsson's results with those obtained experimentally (see section on Photoelastic Method) may be explained by his further

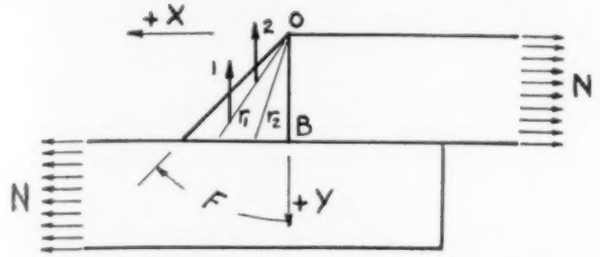

Fig. 18—Single Lap and Fillet Welded Joint with 90° Root Angle. Streletzky and Nikolaieff[50]

assumption that there is a linear distribution of normal stress and a parabolic distribution of shear stress on any sections; x = constant and y = constant.

A procedure similar to Goelzer's was adopted by Rosenthal[64] who believed that the assumption of constant distribution of stress along the length of the weld was justified by the fact that actual tests showed the rupture load of end fillet welds to be nearly proportional to their lengths. Yet he, with many others, also emphasizes the magnitude of the change in stress distribution that may be caused by inelastic action. Rosenthal shows that in the particular case of loading for which the stress distribution along the tension face of the end fillet is linear, the resultant in the strap, assumed to act normal to the tension face, acts at $^1/_3$ the thickness of the strap from the surface of the plate. In this case the shear stress is

$$T_{xy} = \frac{P_y}{L\, m^2\, E^2}$$

where P = load on joint

y = distance in the direction of the strap parallel to the shear face, and measured from the tension toe of the fillet weld
L = leg of weld on shear face
m = tangent of the angle of the weld at the tension toe
E = thickness of strap = leg of weld on tension face.

Rosenthal points out that the notch effect at the root is not accounted for by the stress function. Extensometer measurements (not extensive) tended to confirm the theoretical results.

The method of designing end fillet welds on the basis of the Huber-Hencky theory of failure (theory of constant energy of distortion) has been developed by Kist[111] and is embodied in the tentative welding standards of the Netherlands. The basis of the method is that the stress P effective to cause yielding at a point is related to the normal (S) and shear (T) components by the relation

$$P = \sqrt{S^2 + 3T^2}$$

The total permissible load on the joint is then computed using arbitrary values for the inclination of the axes of principal stress in the critical section to the legs of the weld.

The method simply takes account of the variable inclination of the line of action of the load to the critical or throat section of the fillet. The inclination is greatly affected by the extent to which the straps overlap the main plates. Kist emphasizes the effect of the gap between the main plates in causing bowing of the straps but does not analyze the problem quantitatively. The tests on end fillets made by Jensen in 1934 provide much of the experimental support for the strain-energy method.

The most recent attempt to determine the stress distribution appears to have been made by Gerbeaux.[114] Besides developing elementary formulas for the strength of commonly used end fillet joints, he adopts the procedure of Goelzer to map the stress distribution in double-strap symmetrical end fillet joints. He assumes that each weld of such a joint can be treated independently as a single-lap end fillet in Goelzer's sense, one-half the applied force acting at a distance of $^1/_4$ the thickness of the main plate on each weld. The assumption is crude and the results (isoclinics radiating as a sheaf of straight lines from the tension toe of each weld) do not agree with those determined by photoelastic methods. Gerbeaux concludes that his equations for stress distribution in end fillet joints are very approximate, a conclusion which may be applied indeed to the entire end fillet problem at the present time.

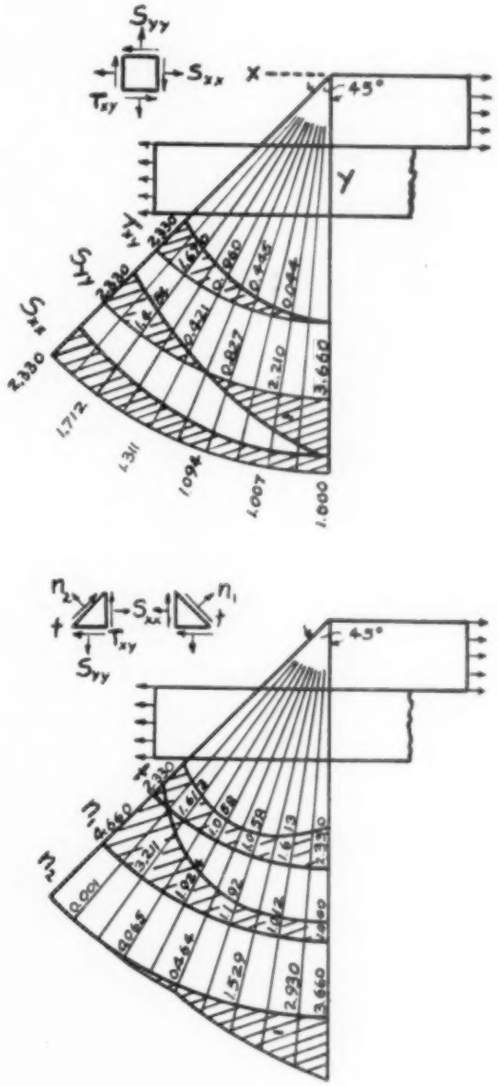

Fig. 19—Calculated Stress Distribution in 45° End Fillet Welds. Streletzky and Nikolaieff[50]

The numerals denote the multiple of the average normal stress acting in plate or strap. The values remain constant along a radius vector. The stresses S and η are tensile except in the negative (−) regions.

Inclined Fillet-Welded Joints

A first-grade attempt to determine the stress distribution in fillet welds inclined to the direction of applied load has not been made. Blackwood[126] analyzed the stresses in inclined fillets at an angle of 0 to 90° to the applied load on the basis of resolution of forces and Strength of Materials. His theoretical plot of angle of inclination *vs.* strength agreed fairly well with experimental strength tests of inclined fillets. But a straight line drawn between the strengths of side (0°) and end (90°) fillets fitted the experimental results even more closely.

Other Types of Fillet-Welded Joints

The stress distribution in box joints (combined end and side fillet welds) has been investigated theoretically by Pilgram[127] and Kalina.[128] In addition to tacit assumptions that eccentricity was absent and Hooke's law was obeyed, Pilgram assumed that the plate was rigid and did not share in the deformation of the welds. He derived a stress function, expanded it as a Fourier series, and arrived at an equation of the cosh type to represent the stress distribution. The proportion P of the load in the strap carried by the end fillet weld is given in Table 10 as a function of the ratio of the length B of the end weld to the length L of one of the two side fillet welds. The remainder of the load is divided equally between the two side fillet welds. The ratio of the maximum normal stress in the end fillet weld to the stress assumed uniformly distributed in the strap external to the joint is given in Table 11. Pilgram's results are not in agreement with Müllenhoff's[129] statement that in box welds almost all the load is carried by the end fillet until it has yielded, after which all the stress is carried by the side fillets. Müllenhoff does not attempt to prove his statement but recommends that box welds should be designed on permissible loads for side fillet welds.

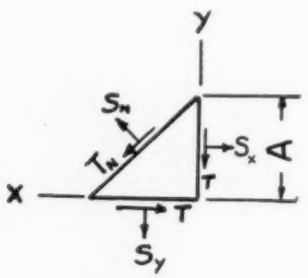

Fig. 20—Notation Used by Olsson[125]

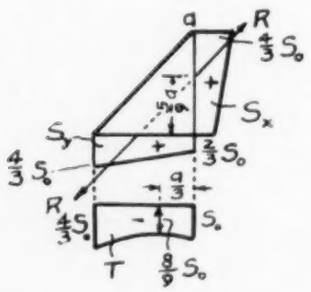

Fig. 21—Calculated Stress Distribution on the Legs of a 45° End Fillet Weld

S_x = normal stress parallel to shear leg
S_y = normal stress perpendicular to shear leg
T = shear stress along shear leg
S_o = average normal stress in each strap. Olsson[125]

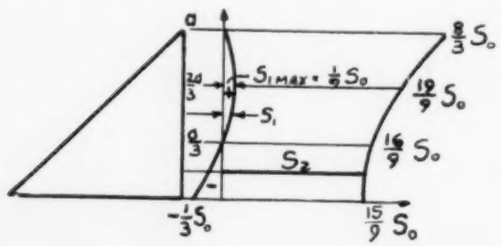

Fig. 22—Calculated Distribution of Principal Stresses S_1 and S_2 in a 45° End Fillet Weld. The lines of constant principal stress are parallel S_1 and perpendicular S_2 to the hypotenuse of the weld. S_o = average normal stress in each strap. Olsson[125]

Table 10—Proportion of Load Carried by End Fillet in Box-Welded Joints. Pilgram[127]

B/L	0	0.5	1	2	3	Infinity
P	0	0.06	0.28	0.58	0.72	1

Table 11—Maximum Normal Stress in the End Fillet Weld of a Box-Welded Joints. Pilgram[127]

B/L	0.5	1	2	3	Infinity
Ratio	0.07	0.41	0.85	0.95	1

The analysis of single-lap box-welded joints given by Kalina[128] is based on his analysis of side fillet welds, including the usual assumptions of perfect elasticity, uniform stress distribution in the straps, and absence of eccentricity. A separate detrusion ratio is envisaged for the end fillet weld, and both detrusion ratios must be determined by experiment. The assumption that the outer end of the side fillet must undergo the same deformation as the entire end fillet, provides the key to the solution, which is in the form of a cosh equation. Kalina makes no attempt to determine the values of the detrusion ratios K_F for the end fillets and K_s for the side fillets, but assumes reasonable values for K_F and observes the effect of varying K_s from 0 (completely yielding side fillets) to ∞ (completely rigid side fillets). An example of his results is shown in Fig. 23 for $K_F = 10 \times 10^6$ psi (steel). The top diagram shows the variation of shear per unit length at each point divided by the average shear. The middle and bottom diagrams show the stresses in strap and plate, respectively. The strap has the same cross sectional area as the plate. For more rigid side fillets ($K_F = 15 \times 10^6$ psi) the proportion of load carried by the end fillet is greater and the stress concentration at the beginning of the side fillet is also greater. Kalina has prepared a number of charts showing the proportion of the load on a box weld carried by the end fillet for all values of K_s, for ratios of length to breadth of strap of 1 to 4.5, for $K_F = 10$ and 15×10^6 psi and for ratios of cross sections of plate to strap of 1 and 2. For values of K_F from 10 to 15×10^6 psi and $K_s = 10$ to 30×10^6 psi (reasonable values) Kalina's charts show that the proportion of load carried by the end fillets is 50 to 400% greater than that computed on the basis (standard German practice) that the end weld can be regarded as an additional length of side fillet.

The experimental results quoted by Bierett[131] are in good qualitative agreement with Kalina's theoretical results. As Kalina points out, the change in stress distribution in strap and plate due to the addition of an end fillet to side fillet welds is considerable but is not accounted for by his formulas. Tatur and Rykalin[145] found that, as a first approximation, the detrusion ratios are independent of size of weld and plates. The detrusion ratio for side fillets is $G/_2$, for end fillets $E/_2$, where G and E are the moduli of elasticity in shear and

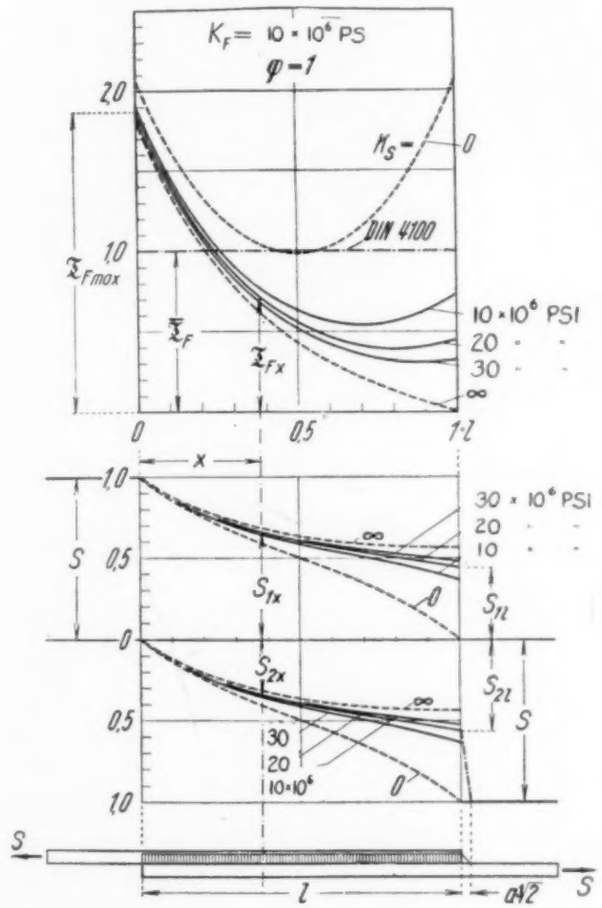

Fig. 23—Calculated Stress Distribution in Box Welds for Different Values of K_s

Top Diagram. Distribution of Shear T_F Along Side Fillet Weld

Middle Diagram. Distribution of Normal Stress S_1 in Strap

Lower Diagram. Distribution of Normal Stress S_2 in Plate. Kalina[128]

normal stress, respectively. Nomograms for computing side, end and box fillets on the basis of the cosh equation are given by these investigators.

An analysis of the forces in T joints has been made by Mies,[130] and Herrfeldt[132] has made an approximate computation of the stress distribution in a wedge-shaped combined butt and fillet weld (a diagonal butt weld with half of the weld of the fillet type). Edge fillet-welded joints and other less commonly used types of fillet welds do not appear to have received theoretical consideration.

Fillet Welds in Bending

(Moment Resisting Fillets)

Experimental

Although a large number of tests on structures involving moment-resisting fillet welds have been made, very little has been done toward determining the stress and strain distribution in fillet welds subjected solely to bending, or for that matter, to torsion. For example, Rosenthal[133] determined the ultimate strength of fillet welds in bending and stated that, under the same load, a fillet-welded joint is as strong in bending as in tension. He made no observations of stress distribution. Danilov[134] also tested fillet welds in bending, using several types of channel to column and channel to channel connections. His results were in fair agreement with three different formulas similar to Albert's[135] suggested to predict ultimate strength.

The strain distribution in moment resisting fillet welds has been determined by only two investigators: Screiner[136] and Griffel.[137] Schreiner determined the strain in the tension ends of symmetrical double strap end fillet welds under pure external bending moment (zero vertical shear) by means of Huggenberger extensometers over a $^1/_2$-inch gage length. Since the extensometer was set parallel to the hypotenuse of the weld, the instrument measured the strain due to one of the principal stresses, which is almost at right angles to the cross section of the weld. The extensometer was placed 80% of the distance from the root of the weld to the hypotenuse. The welds were $1^1/_2$ to 10 in. long, and $^3/_8$-or $^1/_2$-inch legs. Straps and plates were proportioned so that they had not reached the yield point when the welds failed. The position of the neutral axis was determined by means of a Whittemore strain gage. The specimens were whitewashed to indicate yielding.

Schreiner found that the Johnson elastic limit of the tension ends of the welds occurred at a stress of about 27,000 psi (about the same as that of all-weld-metal in direct static tensile tests) beyond which the strain-moment curve bent slowly away from proportionality. It was found that the neutral axis of the weld coincided with the center of the length. Hence, a triangular distribution of stress along the length of the weld was realized below the elastic limit. He assumed that as the load is progressively applied beyond the point at which the extreme end of the fillet yields, the portions of the weld nearer the neutral axis progressively reach the yield strength. The triangular stress distribution diagram is therefore replaced by a rectangular distribution. But the distribution of strain was considered to remain triangular throughout because plate and straps had to remain in contact. On this basis the average strain undergone by any section of the weld could be calculated.

The calculated strain does not take account of the irregular distribution of the strain from root to face, because Schreiner found that the measured strain, assumed uniformly distributed on lines parallel to the hypotenuse, averaged only 1/1.63 of the calculated strain (Fig. 9 in Schreiner's paper). Assuming a parabolic variation of strain and stress along the legs of the weld so that the area under the calculated strain equals that bounded by the parabola, which has its axis slightly beyond the boundary of the section, Schreiner found that the maximum strain at the root was nearly twice (1.96 times) the calculated strain and three times the strain at the hypotenuse. The factor 1.96 compares with 2.44 shown by photoelastic investigation. The difference is probably accounted for by the assumption

of parabolic variation, which was not found in the photoelastic study. Schreiner concluded that up to the elastic limit the ordinary design formulas give the average stress condition in the weld.

Once the yield point is exceeded the difference between the stresses at the face and root of the weld decreases. The stress is practically uniformly distributed over the cross section of the weld at the loading which causes scaling of the whitewash on the surface of the weld. Schreiner observed the inelastic distortion of the weld by noting the bending of originally straight tool marks on the end cross section of the weld.

A determination of the strain distribution along the length of fillet welds in beam-column connections, was made by Griffel.[137] The beams, Figs. 24 and 25, were tested in three-point loading. The strain distribution along the fillet welds at the top of each end of the beams was determined by means of Huggenberger extensometers arranged parallel to the hypotenuse of the weld, as in Fig. 26. Measurements were made at five points along the weld. The stress distribution under two loads for each beam is shown in Figs. 24 and 25, strain being converted directly to stress by means of Young's modulus. The values plotted are averages of measurements at opposite points. Deviations as large as 25% from the mean occurred. Like Schreiner, Griffel assumed that strain was uniformly distributed along lines parallel to the hypotenuse of the weld. The results are similar in form to Rosenthal's[135] for end fillets in direct tension, the ends of a fillet being less highly strained than the middle, and the distribution being symmetrical about the middle of the length of the weld. From these results it may be deduced that the ends of end fillet welds should not be the starting points of fatigue failure, provided the load is applied in a plane perpendicular to the lengths of the fillets.

Theoretical

The theoretical investigations of moment-resisting fillet-welded joints have been largely devoted to evolving design formulas. Albert[138] deduced design formulas for eccentrically loaded end, side and box fillets on the basis of uniform stress distribution on the throat of the weld. Hamann[138] has drawn a number of useful design charts for eccentrically loaded fillets, and Shedd[61] gives general rules for the design of fillet welds in I-beam connections. Kommerell[139] has determined the moment distribution in side fillet welded strengthening plates on beams.

A detailed theoretical analysis of the stress distribution along the welds of box-welded lap and double-strap joints has been made by Ježek[140] on the basis of linear distribution of bending stress in the strap. He also assumed that effects due to non-coincidence of line of action of load in plate and strap might be neglected, and that there is direct proportionality between load per unit length of weld and average displacement. Two displacement coefficients are thus called into being, both to be determined experimentally for a given type of joint. Coefficient K applies to displacements parallel to the length of the weld, whether side or end fillet. Coefficient K_1 applies to displacements perpendicular to the length of the weld.

The analysis leads to complicated equations of the cosh type which may be simplified if the end weld is more than twice as long as the side weld. In this case it may be assumed that the side fillet is stressed along its length only.

In applying the formulas developed to a symmetrical double-strap box weld of conventional type, it was found, assuming both displacement coefficients had the value 14,500,000 psi, that the stress distribution in the side weld is non-uniform. The maximum shear stress in the side weld occurred at the inner (free) end, and was 180% greater than the minimum which occurred near the junction with the end weld. The stresses in the end weld were relatively small. By assuming $K_1 = 29 \times 10^6$, $K = 14.5 \times 10^6$ psi the stress distribution was not altered to a large extent.

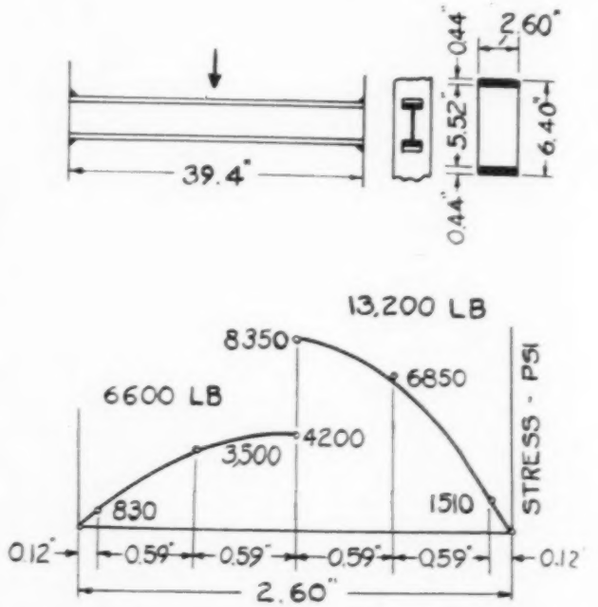

Fig. 24—Stress Distribution Along the Length of a Tension Fillet Weld Connecting an I Beam to a Column. Griffel[137]

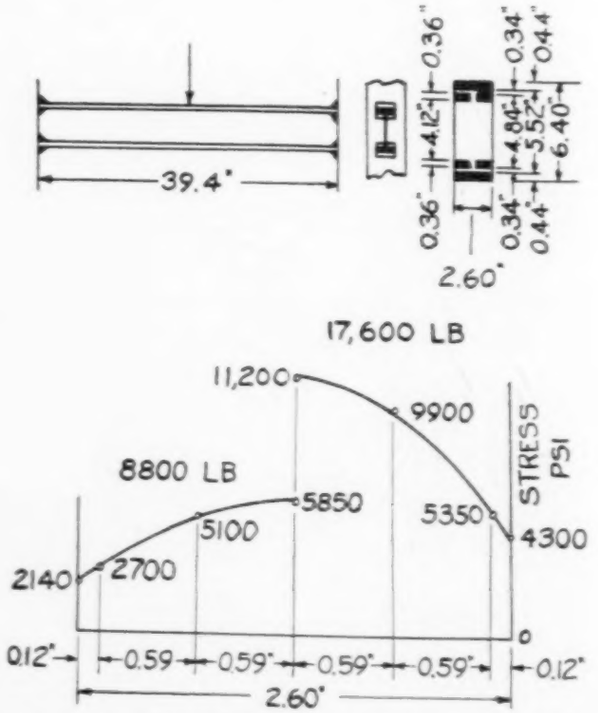

Fig. 25—Stress Distribution Along the Length of the Upper Tension Fillet Weld Connecting an I Beam to a Column

Web = 0.24 inch thick. Griffel[137]

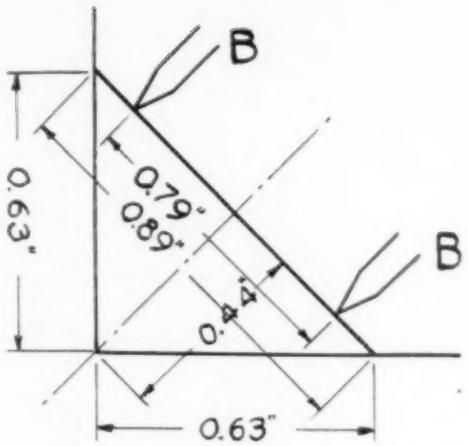

Fig. 26—Position of Tensometer on Fillet Weld in Tension Connecting an I Beam to a Column

B = Leg of extensometer
1000 psi = 0.03 inch at the gage length of the extensometer. Griffel[116]

Sokolow[141] and Kufareff[142] have reported theoretical investigations of the stress distribution in moment-resisting fillet welds. Neither report was available to the reviewers. The feature of the work of both investigators seems to have been the use of matrix calculus. Kufareff's work seems to have been along lines similar to Kalina's[140] (interrupted fillet welds in welded beams). Experimental data on the stress distribution in interrupted fillet welds between the flanges and webs of welded I beams are given by Hochheim.[143]

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Translations

There is available to those interested a transaction of an article entitled: Autogenously Welded Rail Joint Connection (The Bohler X Joint) by H. Melhardt, Vienna, appearing in *Autogene Metallbearbeitung*, **29**, 17–27 (1936), translated by H. Baack, edited by C. E. MacQuigg. The article contains details on construction and cost of the joint, service tests, static bend, impact and fatigue tests of joints, as well as investigations of weld metal, such as static tensile and bend properties and microstructure.

Copies will be sent upon request to the AMERICAN WELDING SOCIETY 33 West 39th St., New York.

Copies of translation of "Thyratron Controllers for Spot and Seam Welding Machines with Rigid Synchronization," by S. I. Emdin (Engineer) and A. S. Egorov-Kuzmin (Engineer), in abstract form, are also available at the AMERICAN WELDING SOCIETY.

An extended abstract of a paper entitled "Calculation of Welds under Consideration of Constant Deformation Energy" by Prof. N. C. Kist, Delft, Holland, has been made by Prof. C. Jensen, Lehigh Univ. There are available a limited number of copies of this abstract, which deals principally with design of fillet welds. Prof. Kist presented his paper at the Second Congress of the International Association for Bridge and Structural Engineering, October 1936.

Erratum

We announce with regret that in making corrections in Table 1, page 3 of the Supplement to the February Journal, the printer inadvertently indicated wrong table headings for a portion of Table 1. That portion relating to the properties of Copper-Molybdenum Steel and Copper-Nickel-Molybdenum Steel is therefore repeated below in corrected form.

Copper-Molybdenum Steel

Average of 5 Test Specimens

C	Molyb.	Copper
0.25	0.25	1.40

	Yield Weld psi	Yield Parent psi	Ult. psi	% Red of Area	Angle of Bend	Location Fracture Break
As-Welded	79,870	90,760	93,740	28	180	Weld
950° F. 1 Hr.	88,390	95,270	98,740	31	141	Weld

Copper-Nickel-Molybdenum Steel

C	Ni	Copper	Molyb.
0.23	0.75	1.30	0.16

	Yield Weld psi	Yield Parent psi	Ult. psi	% Red of Area	Angle of Bend	Location Fracture Break
As-Welded	80,590	90,880	96,460	29	149	Weld
950° F. 1 Hr.	93,870	102,920	104,620	28.8	147	Weld

Elongation and Reduction are approximate as test specimens were rectangular. Distinct yield points for weld and parent metal were indicated by drop of the beam in testing.

WELDING SYMBOLS

and

Instructions for Their Use

THE AMERICAN WELDING SOCIETY'S SYMBOLS given herein are a development of Weld Symbols in use here and abroad and supersede the Society's former Symbols which were published in bulletin form in 1929 and revised in February 1935. These symbols have been recommended to the American Standards Association for adoption as American Standards

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Introduction

WELDING cannot take its proper place as an engineering tool unless means are provided for conveying the information from the designer to the workmen. Unimportant work may be directed by means of inadequate information on drawings, but when structures, the failure of which would endanger life and property, are to be welded, simple and specific means must be used to convey the ideas of the designer to the shop. Such practices as writing "To be welded throughout" or "To be completely welded" on the bottom of a drawing, in effect, transfer the design of all attachments and connections from the designer to the welding operator, who cannot be expected to know what strength is necessary. This practice in addition to being highly dangerous is also costly, for certain shops, in their desire to be safe, use much more welding than is necessary.

These symbols provide the means of placing complete welding information on drawings. Even though the legends, numerical data and the instructions involve a considerable mass of material, nevertheless the successful use of the scheme depends so little on the memory, that hardly more than one reading of the instructions is necessary to obtain a working understanding of the system. In practice many companies will probably need only a few of the symbols, and if they desire, can make up their own legends to suit themselves, selecting such parts of the scheme as fit their needs and neglecting the others. If this is done universally, we shall all be speaking the same language even though some use but a few of the symbols contained herein.

In these symbols, the general principle of having the most usual form of any weld require a minimum of numerical data, has been followed. When a weld departs from the user's standard, supplementary data are necessary. This makes the user's standards a part of the symbol system, and means that the exact interpretation of the symbols will vary slightly among the different users.

It will be seen from Fig. 1 that the symbols are ideographic; that is, they are picture-writing symbols; they show graphically the type of weld required. The individual basic symbols become the building blocks with which compound symbols to indicate complicated welded joints composed of many welds, can be constructed. Every weld in the joint must be shown.

The scheme is a shorthand system whereby a tremendous volume of information may be accurately indicated with a few lines and a minimum amount of numerical data. This is illustrated in Fig. 4, where the words necessary to convey the information given by the symbol would make a very long paragraph.

The distinction between the V- and bevel-grooved welds and the U- and J-grooved welds is not great. The draftsman should take sufficient care in the making of these particular symbols so that they do not become confused with each other.

The field weld symbol is the black dot used by the structural industry to indicate field riveting. In the case of work actually erected in the field, just what constitutes field welding is simple. In the case of work done in the shop, yet done in the actual erection of the final product, the case is not so simple. An illustration of this obtains when work is done in the shop on an assembly line, such as is used in the automobile or car building industries. In this case, the individual user must decide for himself whether such erection welding is shop welding or field welding.

Appropriate finish marks have been found to be necessary, however, recommendations as to what finish marks shall be used are not strictly within the province of the Committee. As soon as the American Standards Association has definitely decided upon a system of finish symbols, it will be desirable for all concerned to adopt that system. In the meantime, however, a suggestion with regard to finish marks is made. It will be noted on page 11 of the Instructions that these finish marks merely suggest the means of finishing; that is, whether chipping, grinding or machining be used, and not the degree; they do not say whether a weld is to be rough or finish machined, rough or smooth ground, etc. Any such fine distinction must be made in the user's standard manner until such time as a national standard is established.

The location of the Symbols, numerical and other data on the reference line always has definite significance. This is depicted in Fig. 3 in which the standard manner of placing information on the *symbols* is shown diagrammatically.

A new feature incorporated in this revision is the use of the tail of the reference line for referring to the welding specification reference to be used in the making of the weld. If a welding operator knows the size and type of weld, he has only part of the information necessary for the making of that weld. The process, type and make of filler metal that is to be used, whether or not peening, or chipping are required and other pertinent data must be known before he can start the work. The specification to be placed in the tail of the reference line at present will have to be handled by each individual Company which will set up its own requirement in any manner it sees fit, and it is hoped that in time a national set of standards will be prepared to coordinate the various specification processes in use. Steps in that direction are now under

consideration. Such matters as stress-relief annealing, radiographing and final cleaning of the product cannot be referred to on the symbol because such treatment is applied to the product as a whole and not to a particular weld.

The committee wishes to make two points very clear. *First*—that the individual company may use just as much or as little of the system as it sees fit, and *second*—that the system may be used in any of the various ways listed below:

(*a*) All Symbol legends and explanatory matter may be issued as Company Standards on sheets separate from the drawing in question; that is, the draftsman may have explanatory supplementary sheets as well as machinists, welding operators, inspectors, etc.

(*b*) Legends and specification references may be placed on the drawing so that the latter is completely self-explanatory.

(*c*) In either of the above cases, the welds may be drawn in sections and the Symbols give only that information that is not obvious, such as size of weld, length of increment, etc.

(*d*) In either of the cases (*a*) and (*b*) above the welds may not be drawn in section and complete information conveyed by symbols.

(*e*) The symbol legends, specification references and standard notes may be printed on the tracing or may be placed on tracings or prints by rubber stamps or any other means.

The resistance welding legend can be shown on drawings together with the fusion welding legend if desired.

The principal difference between the fusion and resistance welding symbols as revised herein is that the fusion welding symbols designate the size of the weld, whereas the resistance welding symbols call for the strength of the required weld. This difference is necessary because in spot, projection and seam welding, the weld is inaccessible and therefore cannot be gaged as in fusion welding.

The main committee desires to express its appreciation to the Symbols Subcommittee and particularly to its chairman Mr. Leon C. Bibber and his assistant Mr. Julius Heuschkel for their earnest and long work; to the Carnegie-Illinois Steel Corp. for furnishing its technical and clerical facilities; to Mr. J. W. Owens, Chairman of the Main Committee and the U. S. Navy, the American Standards Association and all other cooperating bodies and concerns for their whole-hearted support.

The personnel of the Society's Nomenclature, Definitions and Symbols Committee and its Symbols Subcommittee, as at present constituted, is as follows:

*James W. Owens (*Chairman*, Main Committee)—Fairbanks, Morse and Co.
*L. C. Bibber (*Chairman*, Symbols Subcommittee)—Carnegie-Illinois Steel Corp.
*R. W. Clark (*Chairman*, Chart and Definitions Subcommittee)—General Electric Co.
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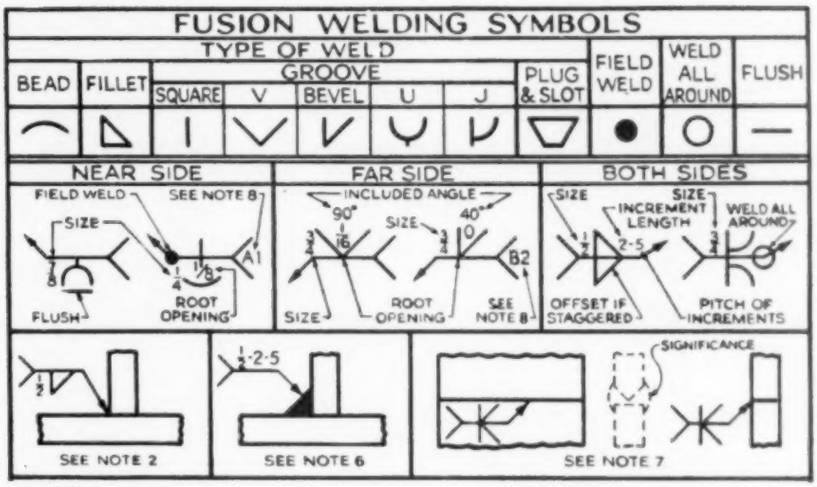
*Naval Gun Factory—C. M. Underwood
*C. W. Obert—International Acetylene Association
*H. M. Priest—(Railroad Research Bureau, U. S. Steel Corporation Subsidiaries)

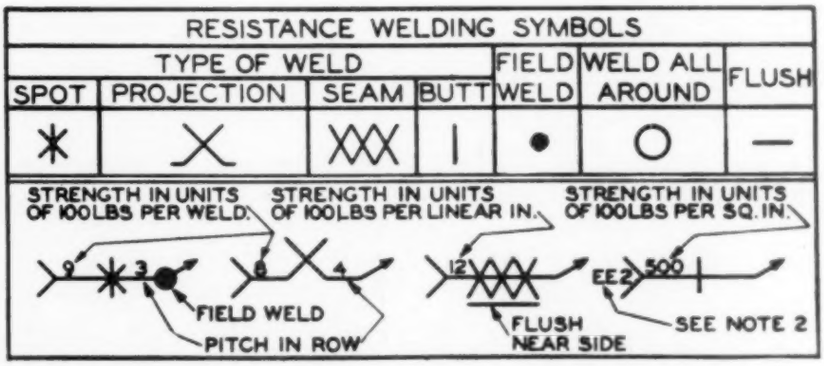
* Member Symbols Subcommittee of the AMERICAN WELDING SOCIETY, Nomenclature, Definitions and Symbols Committee, and of the Symbols Subcommittee of Sub-Group Z-32 of the American Standards Association.



1. In plan or elevation, near, far and both sides locations refer to nearest member parallel to plane of drawing and not to others farther behind.
2. In section or end views only, when weld is not drawn the side to which arrow points is considered near side.
3. Welds on both sides are of same size unless otherwise shown.
4. Symbols govern to break in continuity of structure or to extent of hatching or dimension lines.
5. All welds are continuous and of user's standard proportions and all except V- and bevel-grooved welds are closed unless otherwise shown.
6. When welds are drawn in section or end views, obvious information is not given by symbol.
7. In joints in which one member only is to be grooved arrows point to that member.
8. Tail of arrow used for specification reference.

Note: All dimensions are in inches.

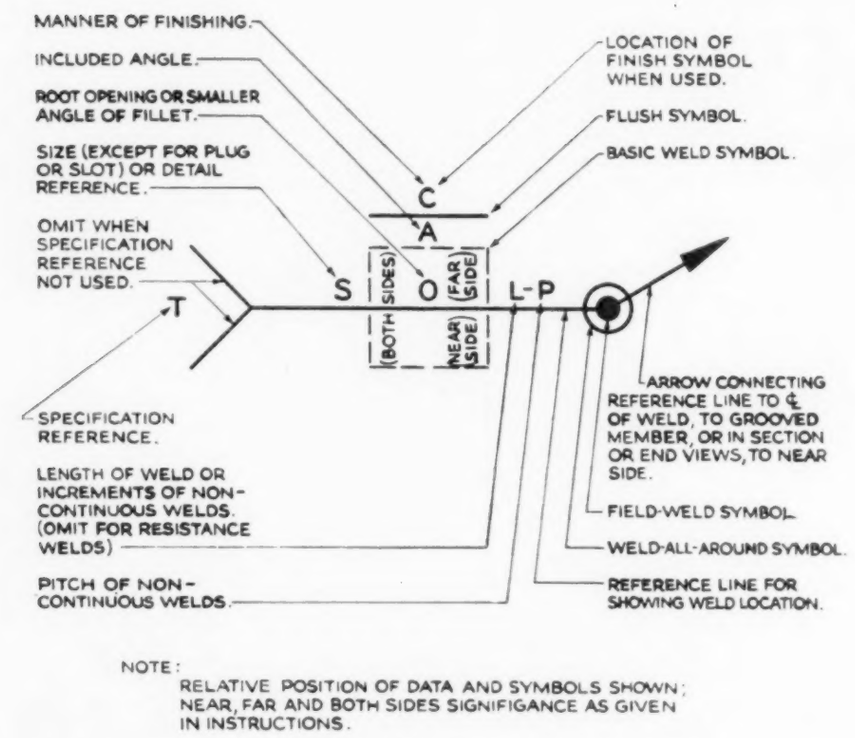
Fig. 1—Legend for Use on Drawings Specifying Fusion Welding



1. Symbols govern to break in continuity of structure or to extent of dimension lines.
2. Tail of arrow used for specification reference.

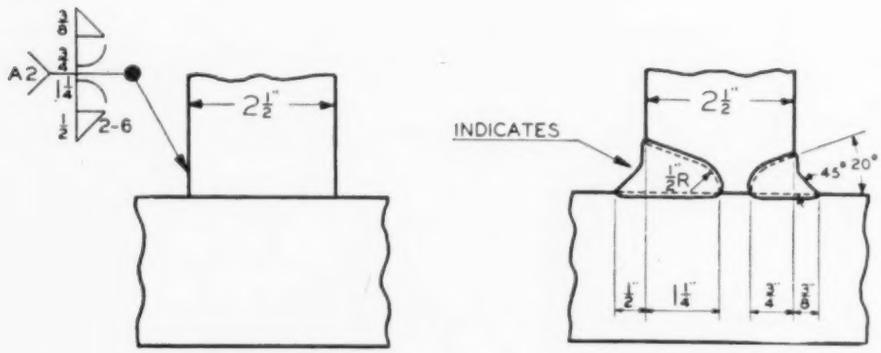
Note: All dimensions are in inches.

Fig. 2—Legend for Use on Drawings Specifying Resistance Welding


MANNER OF FINISHING.
INCLUDED ANGLE.
ROOT OPENING OR SMALLER ANGLE OF FILLET.
SIZE (EXCEPT FOR PLUG OR SLOT) OR DETAIL REFERENCE.
OMIT WHEN SPECIFICATION REFERENCE NOT USED.
SPECIFICATION REFERENCE.
LENGTH OF WELD OR INCREMENTS OF NON-CONTINUOUS WELDS. (OMIT FOR RESISTANCE WELDS)
PITCH OF NON-CONTINUOUS WELDS.
LOCATION OF FINISH SYMBOL WHEN USED.
FLUSH SYMBOL.
BASIC WELD SYMBOL.
ARROW CONNECTING REFERENCE LINE TO ℄ OF WELD, TO GROOVED MEMBER, OR IN SECTION OR END VIEWS, TO NEAR SIDE.
FIELD-WELD SYMBOL.
WELD-ALL-AROUND SYMBOL.
REFERENCE LINE FOR SHOWING WELD LOCATION.
C
A
S
O
L-P
T
(BOTH SIDES)
(FAR SIDE)
(NEAR SIDE)

NOTE:
RELATIVE POSITION OF DATA AND SYMBOLS SHOWN; NEAR, FAR AND BOTH SIDES SIGNIFIGANCE AS GIVEN IN INSTRUCTIONS.

Fig. 3—Standard Location of Information on Welding Symbols



SKETCH 1 SKETCH 2

Interpretation of symbol:

Double-fillet-welded, partially-grooved, double-J, tee-joint with incomplete penetration. (Type of joint shown by drawing). Grooves of standard proportions (which may be $^1/_2$ in. R, 20° included angle, root edges in contact before welding) $^3/_4$ in. deep on far side and $1^1/_4$ in. deep on near side. $^3/_8$ in. continuous fillet weld on far side and $^1/_2$ in. intermittent fillet weld on near side with increments 2 in. long, spaced 6 in. center-to-center. All fillets standard 45° fillets. All welding done in field in accordance with welding specification number A2 (which may require, for example, that weld be made by manual D.C. shielded metal-arc process using high-grade, covered, mild steel electrode; that root be unchipped and welds unpeened but that joint be preheated before welding).

Fig. 4—Comparison Between Symbolic and Verbal Methods of Conveying Welding Information

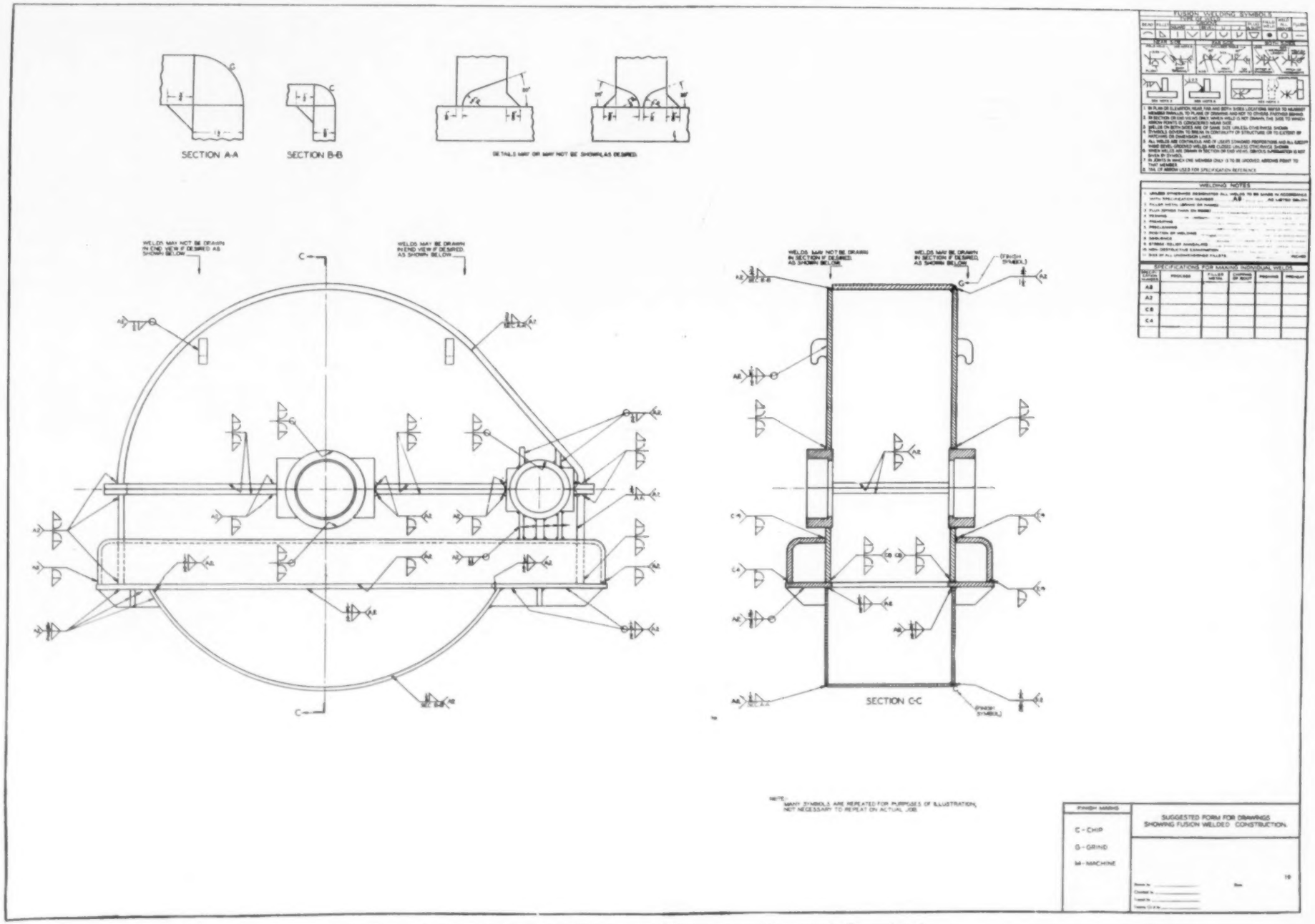

SECTION A-A
SECTION B-B
DETAILS MAY OR MAY NOT BE SHOWN, AS DESIRED
FUSION WELDING SYMBOLS
WELDING NOTES
SPECIFICATIONS FOR MAKING INDIVIDUAL WELDS
WELDS MAY NOT BE DRAWN IN END VIEW IF DESIRED AS SHOWN BELOW
WELDS MAY BE DRAWN IN END VIEW IF DESIRED, AS SHOWN BELOW
WELDS MAY NOT BE DRAWN IN SECTION IF DESIRED, AS SHOWN BELOW
WELDS MAY BE DRAWN IN SECTION IF DESIRED, AS SHOWN BELOW
SECTION C-C
NOTE: MANY SYMBOLS ARE REPEATED FOR PURPOSES OF ILLUSTRATION, NOT NECESSARY TO REPEAT ON ACTUAL JOB
FINISH MARKS
C - CHIP
G - GRIND
M - MACHINE
SUGGESTED FORM FOR DRAWINGS SHOWING FUSION WELDED CONSTRUCTION

Fig. 5—Typical Drawing Showing Use of Symbols

Instructions for Use of Welding Symbols

I General

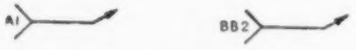
(*a*) Do not use the word "weld" as a symbol on drawings.

(*b*) Symbols may or may not be made freehand as desired.

(*c*) Inch, degree and pound marks may or may not be used as desired.

(*d*) The symbol may be used without specification references or tails to designate the most commonly used specification when the following note appears on the drawing:
"Unless otherwise designated, all welds to be made in accordance with welding specification No. —."

(*e*) When specification references are used, place in tail, thus:



(*f*) Symbols govern weld to a break in continuity of the structure, or to extent of hatching or dimension lines.

(*g*) Faces of welds assumed to have user's standard* contours unless otherwise indicated.

(*h*) Faces of welds assumed not to be finished other than cleaned unless otherwise indicated.

(*i*) All except plug, spot and projection welds assumed continuous unless otherwise indicated.

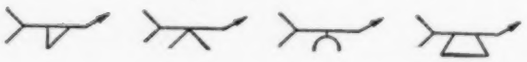
(*j*) All except V- and bevel-grooved welds are assumed to be closed unless otherwise indicated.

II Fusion Welds

1. General

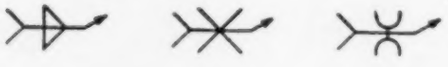
(*a*) Do not put symbol directly on lines of drawing; place symbol on reference line and connect latter to joint with *arrow*, thus:

(*b*) For welds on near side of member, show symbol on near side of reference line, face toward reader, thus:



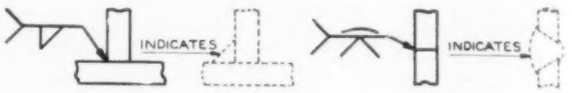
(*c*) For welds on far side of member, show symbol on far side of reference line, face away from reader, thus:

(*d*) For welds on both sides of member, show symbols on both sides of reference line, faces toward and away from reader, thus:



(*e*) In plan or elevation the near side, far side and both sides locations refer to the nearest member parallel to the plane of the drawing, or approximately so, and not to others farther behind.

(*f*) In section or end views only, when the welds are not drawn, the side to which the arrow points is considered the near side of the member and the locations of the symbols on the reference line have their usual significance. In the case of joints made with one weld only, connect arrow to side to be welded and always use near side symbol.



(*g*) In joints in which one member only is to be grooved, show arrow pointing to that member, thus:

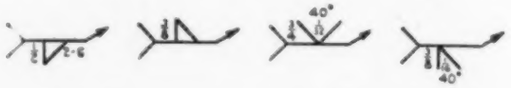
(*h*) Read symbols from bottom and right-hand side of drawing in the usual manner and place numerical data on vertical reference lines so that reader will be properly oriented, thus:

(*i*) Show symbol for each weld in joints composed of more than one weld, thus:

(Give numerical data in proper location with regard to each symbol.)

(*j*) In complicated joints requiring large compound symbols, two separate sets of symbols may be used if desired.

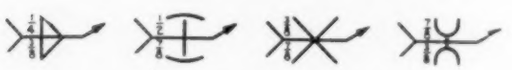
(*k*) Show dimensions of weld on same side of reference line as symbol, thus:



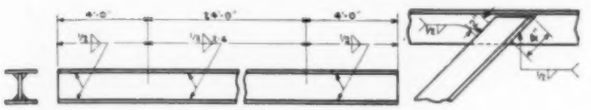
(*l*) Show dimensions of one weld only, when welds on both sides of member are of the same size, thus: (see II, 3*a*)

* See reference in introduction.

(*m*) Show dimensions for both welds when welds on opposite sides of member are of different sizes, thus: (see II, 3*a* and 3*d*)



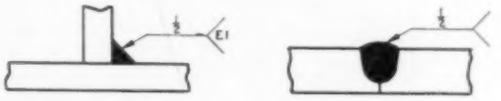
(*n*) Indicate specific lengths of welds in conjunction with dimension lines, thus:



(*o*) Show the welding between every break in the continuity of the structure, thus:

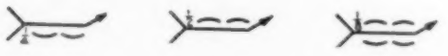
(*p*) When it is desired to show extent of welds by hatching, use one type of hatching with definite end lines, thus:

(*q*) If actual outlines of welds are drawn in section or end elevation, basic symbol is not necessary to show type and location; size or other numerical details only need be given, thus:



2. Bead Welds

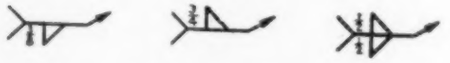
(*a*) Show bead welds used in building up surfaces (size is minimum height of pad), thus:



(*b*) When a small but no specific minimum height of pad is desired, show thus:

3. Fillet Welds

(*a*) Show the size of fillet weld (length of shorter leg), thus:



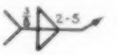
If the majority of fillet welds are of the same size, the dimensions of that size may be omitted from the symbols and placed in note 11, Fig. 5 which reads as follows, "Size of undimensioned fillets—inches." Show the dimensions of all fillets other than that size on the symbols.

(*b*) Show specific length of fillet weld or increment, after size, thus:

(*c*) Show center-to-center pitch of increments of intermittent fillet welds after increment length, thus:

(*d*) Use separate symbol for each weld when intermittent and continuous fillet welds are used in combination.

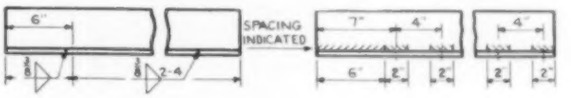
(*e*) Show chain intermittent fillet welds, thus:



(*f*) Show staggered intermittent fillet welds, thus:

(*g*) Pitch of staggered intermittent fillet welds measured between centers of increments on one side of member.

(*h*) Increments and not spaces assumed to be placed at both ends of length governed by an intermittent fillet weld symbol unless otherwise indicated, thus:



(*i*) Faces of fillet welds assumed to be at 45° from legs unless otherwise indicated.

(*j*) When face of fillet weld is at any other angle than 45°, show smaller angle, thus (size is length of shorter leg):

(*k*) Show position of longer leg of fillet weld by placing symbol in same relative position that obtains on work, thus:

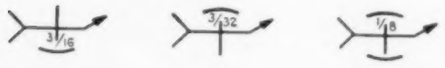
4. Grooved Welds

(*a*) Show side from which square-grooved weld is made by bead or flush symbol, thus: (see III, 4*a*; IV, *c*; and IV, *e*)

(*b*) Total penetration of square-grooved welds assumed to be complete unless otherwise indicated.

(*c*) Show size of square-grooved welds (depth of penetration) when penetration is less than complete, thus:

(*d*) Show root opening of open, square-grooved welds inside symbol, thus:



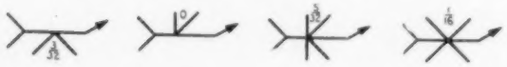
(*e*) Total depth of V- and bevel-grooves before welding assumed to be equal to thickness of member unless otherwise indicated.

(*f*) Show size of V- and bevel-grooved welds (depth of single groove before welding) when grooving is less than complete, thus:

(*g*) Total depth of penetration of V- and bevel-grooved welds assumed complete, unless with usual welding processes, depth of grooving is such that complete penetration is not possible, when depth of penetration is assumed to be depth of groove plus normal penetration. When using welding processes giving abnormal penetration, give information on latter by detail or note. (see IV, *j*)

(*h*) Root opening of V- and bevel-grooved welds assumed to be user's standard unless otherwise indicated.

(*i*) Show root openings of V- and bevel-grooved welds when not user's standard, inside symbol, thus:

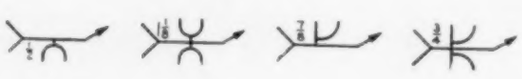


(*j*) Included angle of V- and bevel-grooved welds assumed to be user's standard unless otherwise indicated.

(*k*) Show included angle of V- and bevel-grooved welds when not user's standard inside symbol, thus:

(*l*) Proportions of U- and J-grooved welds assumed to be user's standard unless otherwise indicated.

(*m*) Show size of U- and J-grooved welds (depth of single groove before welding) having user's standard proportions but incomplete penetration, thus:



(*n*) When proportions of U- and J-grooved welds are not user's standard, show weld by detail or reference drawing and use reference symbol thus (see IV, *j*):

(*o*) Show welding done from root side of single grooved welds with bead weld symbol, thus:

5. Plug and Slot Welds

(*a*) Show size of plug and slot **welds** (root opening and root length), thus:

(Root opening equals root **length** for plug welds.)

(*b*) Included angle of bevel of plug and slot welds assumed to be user's standard unless otherwise indicated.

(*c*) Show included angle of bevel of plug and slot welds when not user's standard, thus:

(*d*) Show pitch of plug and slot welds in row, thus:

(*e*) Show fillet-welded holes and slots with proper fillet weld symbols and not with plug weld symbols.

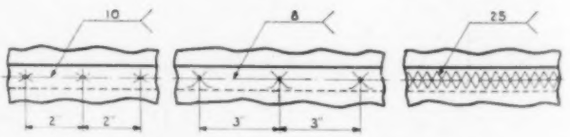
III Resistance Welds

1. General

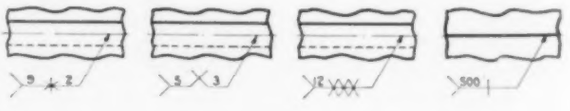
(*a*) Location of symbol on reference line has no significance for resistance welds.

(*b*) Designate resistance welds by strength rather than size (because of impracticability of determining latter).

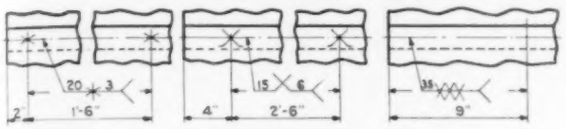
(*c*) Spot, projection and seam weld symbols may be used directly on drawings, thus:



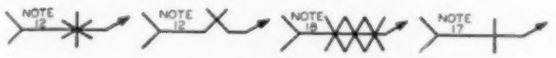
(*d*) When not so used, connect reference line to center line of weld or rows of weld with arrow, thus:



(*e*) Show welds of extent less than between breaks in continuity of structure, thus:



(*f*) When tension, impact, fatigue or other properties are required, use reference symbol, thus: (see IV, *j*)



2. Spot and Projection Welds

(*a*) Show strength of spot and projection welds in single shear in units of 100 pounds per weld, thus:

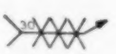
(*b*) Show strength and center-to-center spacing of spot and projection welds in row, thus:

(*c*) Proportions of projections assumed given on drawing.

3. Seam Welds

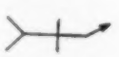
(*a*) Seam welds assumed to be of overlapping or tangent spots. If any spacing exists between spots, welds considered a series of spot welds, and spot symbol should be used.

(*b*) Show shear strength of seam welds in units of 100 pounds per linear inch, thus:



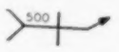
4. Butt Welds

(*a*) Show resistance butt welds without bead weld symbol signifying that weld is not made from any side, but all at once, thus: (see II, 4*a*)



(*b*) Resistance butt welds assumed to be equal to strength of base metal in tension unless otherwise indicated.

(*c*) When a different strength is desired, show strength of butt welds in tension in units of 100 pounds per square inch, thus:



IV Supplementary Symbols

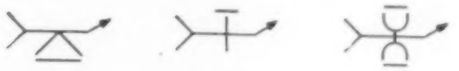
(*a*) Show "field" welds (any weld not made in shop), thus:

(*b*) Show "all around" welds (weld encircling member in so far as possible), thus:

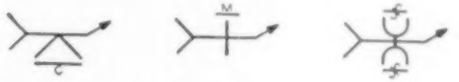
(*c*) The location of the flush and finish symbols have the usual near, far and both sides significance and govern only the sides on which they are shown.

(*d*) Finish marks govern faces of welds only and not base metal either before or after welding.

(*e*) Show fusion welds made flush without recourse to any kind of finishing, thus:



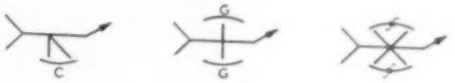
(*f*) Show fusion welds made flush by mechanical means with both flush and user's standard finish symbols, thus:



The following letters are suggested for indicating finishing processes:

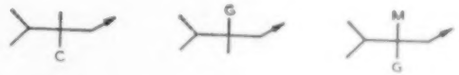
C = Chip
G = Grind
M = Machine

(*g*) Show finishing on face of fusion welds, which need not be flush, with user's standard finish symbols on bead symbol, thus:



(*h*) Show spot, seam or projection welds made practically flush (with minimum indentation), thus:

(*i*) Show resistance butt welds, finished by mechanical means, without flush symbol, thus:



(*j*) Show special welds not covered by any of the above symbols by a detailed section or reference drawing, or give any supplementary information by means of a note and refer weld to section, drawing or note by a reference symbol. Reference symbol has usual location significance, thus:

THE ENGINEERING FOUNDATION
WELDING RESEARCH COMMITTEE

Sponsored by American Welding Society and American Institute of Electrical Engineers

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Strain Measurements in Welded Joints*

By J. L. BANVILLE† and H. A. STORRS**

SOMETIME ago the authors of this paper undertook to develop a new method for measuring the ductility of the metal in a weld while under a series of tensile stresses, in an attempt to determine the entire elastic curve of the metal.

Our experiments were only partially successful but a brief account of our work may suggest ways for extending the investigations so as to accomplish the desired end.

It seemed to us that methods of testing were not keeping pace with the great improvement in the ductility of weld metal produced by the developments of recent years in the art of welding.

For instance, the "free bend" test, so generally used in this country, fails to give the accuracy of determination desired.

Measuring between gage points with a flexible scale over a curved surface seems crude and inexact. Only micrometer measurements of elongation can determine the differences in quality of high grade welds.

Furthermore, in the free-bend test it is required that the bending be stopped when "incipient cracking" appears on the outside of the bend. Since this is indefinite and not always possible to detect, the specifications state that the measurements between gage points is to be corrected by "allowing for the width of the cracks."

Perhaps the reason that few of the other industrial nations have looked with favor on the American "free bend" method is that too much guesswork is involved, and they have resorted to some form of "controlled" bending procedure, making their determination depend on angle of bend and other measurements, such as the "mean bending radius" of curvature of the bend. In one case the ductility of the weld metal is stated to be acceptable if no cracking appears when the specimen is bent to a prescribed angle. In another case the test bar is bent until a crack 5 mm. long appears on the tension side.

There is yet to be mentioned the elongation determination sometimes made in connection with the tension test of a weld specimen. The gage length spans the weld and the gage points may be an inch or two apart, or less even down to the width of the weld itself.

For several reasons this test gives unsatisfactory results. For instance, if the gage points span both plate metal and weld metal, the measurement of total elongation comprises the elongation of two dissimilar metals and there is no way to apportion the total to the individual metals. Again if the points are placed inside the edges of the weld, the gage length may be too short to accurately measure it. Besides that, unless the fracture occurred through the weld, maximum elongation has not occurred within such gage length. If it does occur in the weld, and the broken edges of the fracture be afterward pressed tightly together, a measurement between gage points will include an irregular crack whose estimated width must be subtracted from the actual measurement between gage points.

From the above brief review of some of the principal undesirable features of present methods for testing welds, it seems evident that most of the difficulties and deficiencies would be eliminated if we could (1) so prepare our test specimen that it could be placed in position as for the regular tension test, and then (2) have available a means for continuously measuring the elongation of the weld metal as the stress on it was increased from zero to the ultimate breaking strength of the weld metal; providing also (3) that the fracture could be caused to occur along the longitudinal axis of the weld.

The above statement described the situation that faced us, and indicates the objective we had in view when we began our experiments.

In the following description of our adopted procedure for making a ductility test, we can make the picture clearer by stating definite dimensions of a typical case.

The test specimen is steel plate, $1/4$ inch thick, $1\,1/2$ inch wide and 14 inch long, including a single-or double-

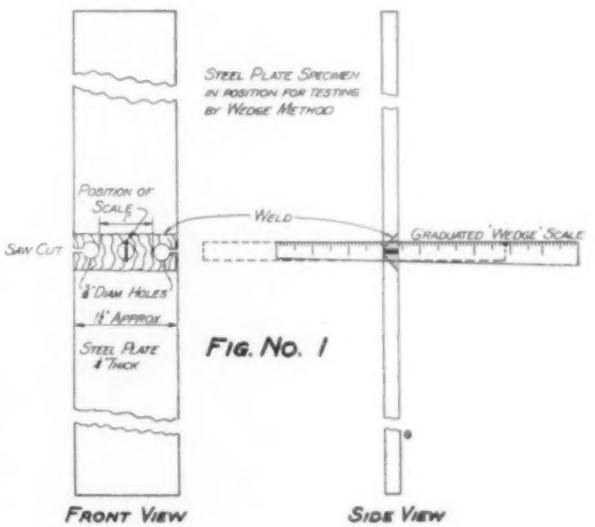


FIG. NO. 1

* Presented before San Francisco Section of the AMERICAN WELDING SOCIETY, April 30, 1937. Contribution to Fundamental Research Committee.

† Engineering Materials Laboratory, University of California.

** Senior Inspector, East Bay Municipal Utility District.

vee weld centrally located. The beads having been ground off flush with the plate metal, three $^3/_{16}$-inch holes are drilled through the axis of the weld, one in the center and the others, one inch apart in the clear, equally spaced each side of the center hole, thus leaving about four-tenths of an inch of weld metal on each side of the center hole. Saw-cuts are then made along the axis of the weld into the outer holes, from the edges of the specimen, as shown in Fig. 1.

When the specimen is placed in the tension machine and stressed, the entire tensile stress must be transmitted by the reduced cross section at the weld. Since the area of this cross section is only about one-half that of the specimen elsewhere in its length, rupture may be expected to occur along the axis of the weld. Thus, will one of our aims be attained.

If an increasing stress be applied, as in the case of a tension test, the weld metal between the holes will stretch and the holes will become eliptical. The increase in the diameter of the hole, measured in the line of stress and at right angles to the axis of the weld, will then be a true measure of the elongation of the weld metal in a length equal to the original diameter of the drilled hole. If the stress is increased until rupture occurs, the elongation determined as rupture occurs will complete the measurement of the ductility of the weld metal.

For measuring rapidly and accurately the distortion of the central hole resulting from the stretch of the weld metal as additional stress was applied, we used a long-taper wedge of thin metal, 10 inches long, carrying a graduated scale of 50 divisions per inch. Before applying any stress, the small end of the wedge was inserted in the center hole, and an initial scale reading was taken at the point of contact of the edge of the hole with the edge of the scale. As stress was applied the hole elongated and the wedge could be inserted further into the hole and new scale readings could be taken. Since the taper of the wedge was in the ratio of 100 to 1, advancing the wedge into the hole an inch beyond its initial position indicated that an elongation of $^1/_{100}$ of an inch had been produced. An advance of a single division on the scale could be easily read and would indicate an elongation of $^1/_{5000}$ or 0.0002 of an inch. For greater accuracy two similar wedges were often used. They were inserted into the hole from opposite sides, one wedge overlapping the other. Then advancing each wedge as the hole elongates and reading the graduations on the back wedge at the end of the overlapping wedge, the scale reading is double the true value corresponding to the elongation of the hole. Hence, if this advance is one division on the scale as read, the actual elongation is $^1/_{10,000}$ of an inch. Thus, do we gain our second objective, viz., accurate measurement of elongation under any stated stress.

Since the nominal diameter of a $^3/_4$-inch hole is 0.1875 of an inch, an increase of $^1/_{10,000}$ of an inch indicates an elongation in the metal of 0.054 per cent, or about one-twentieth of one per cent. This indicates the accuracy possible by this method.

With the specimen so prepared and placed in the testing machine ready for applying tension, the two wedges are inserted in the center hole and an initial reading of the scale is taken at the end of the overlapping wedge. Stress may then be applied in a series of increments, and after each increment of load is applied the wedges are inserted further into the hole and a new reading of the scale

Table 1—Ductility Tests of Welds by the "Wedge" Method

(1) Group No.	(2) No. of Tests	(3) Elongation at Yield	(4) Per Cent at Rupture	(5) Ratio Col. 3/4	(6) Stress (Load in Lb.) At Yield	(7) Stress (Load in Lb.) At Rupture	(8) Ratio Col. 6/7
1	5	0.44	16.9	2.6%	7,600	12,660	60%
2	5	0.71	22.9	3.1%	23,900	34,420	69%
3	3	0.51	21.8	2.3%	9,700	16,330	72%

Group No.	Description
1	Hand butt weld, $^1/_4$-in. plate, bare rod, single V, 2 passes
2	" " " $^1/_2$-in. " coated rod, double V, 3 passes
3	Machine " " $^1/_4$-in. " bare rod, single V, 1 pass

Table 2—Tests for Ductility of Steel Plate Metal

(1) Test No.	(2) Elongation at Yield	(3) Per Cent at Rupture	(4) Ratio Col. 2/3	(5) Stress (Load in Lb.) At Yield	(6) Stress (Load in Lb.) At Rupture	(7) Ratio Col. 5/6	(8) Plate Thickness
19	0.42	40.5	1.04%	7,550	16,150	45.5%	$^1/_4$ in.
26	0.47	42.2	1.11%	7,000	11,800	58.4%	"
35	0.47	42.0	1.12%	7,700	15,400	50.0%	"
44	0.46	45.8	1.00%	15,000	38,500	39.0%	$^1/_2$ in.

Table 3—Steel Plate Tension and Elongation Tests

Test No.	Strength at Yield	Lb. per Sq. In. Ultimate	Elongation by Gage Points	Description
18	30,600	61,650	In 8 in., 20% In 2 in., 40% In 6 in., 23%	$^1/_4$-in. plate, milled 1 in. wide for 9 in. length. Gage points at 1-in. intervals.
45	36,000	61,400	In 2 in., 34.5% In 1 in., 43.5% In $^1/_2$ in., 48%	$^1/_4$-in. plate, milled 1 in. wide for $6^1/_2$ in. length. Gage points at $^1/_2$-in. intervals.
52	34,900	62,600	In 2 in., 37.5%	$^1/_4$-in. plate, milled $^1/_2$ in. wide for $2^1/_2$ in. of its length.

is taken as before. The differences between the initial reading and these later readings are proportional to the stretch or elongation of the weld metal in a length equal to the original diameter of the drilled hole. The following formula is applicable for readings taken in a $^3/_{16}$-inch hole with two overlapping wedges, each having a taper of 100 to 1:

$$\text{Elongation, in per cent} = \frac{B-A}{0.375}$$

A = initial reading (no stress on weld)
B = any later reading (at corresponding stress on weld).

The record of a typical test in our experiments on a double vee weld specimen shows 50 successive increments of load of 500 pounds each, the "A" reading of the scale being 2.62 and the "B" reading 12.30 at the instant of rupture. Hence, the indicated maximum elongation of the weld metal is 25.8 per cent. The scale reading "B," at the elastic limit was 2.86, indicating an elongation of the weld metal of 0.90 per cent. This is three and a half per cent of the elongation at rupture. In the course of the test, it was noted that the elastic limit was reached with a load of 25,000 pounds, which is about 70 per cent of the 35,300 pounds required to cause rupture.

Table 1 presents the results of tests of three groups of weld specimens. In each case three $^3/_{16}$-inch holes were drilled through the axis of the weld and saw-cuts were made into the outside holes, leaving a total width of about 0.8 of an inch of weld metal subject to the applied tensile stress.

By the same wedge method the test results shown in Fig. 2 were obtained for steel plate specimens containing no welds.

Tensile tests of machined steel plate specimens gave the following results by the usual American Society for Testing Materials method, determining the elongation by measurements between gage points.

The results presented in Table 3 were obtained by elongation measurements made with dividers, spanning the juncture of the fractured surfaces which were pressed tightly together.

Thus far, we have been chiefly interested in presenting the "wedge" method for determining the elongation of weld and plate metals under tensile stresses, and have avoided discussion of one aim of our investigations, namely, determining the elastic curves of the metals.

In stating the elongations at yield point and at rupture of the metal we have mentioned only the "load in pounds" as measuring the total stresses applied to the test specimen. Evidently as a basis for comparing one metal with another the true elastic curve of a metal can be drawn only when the "load in pounds" can be evaluated into "pounds per square inch" of stressed area. The introduction of the holes and saw-cuts required by the "wedge" method results in a condition unlike that presented when a steel plate specimen of uniform cross section throughout its entire length is stressed in tension. There results an apparent increase of strength in the contracted sections between the holes in the "wedge" specimen, due probably to the strain hardening effect, resulting from cold working of steel, with the drill and with shrinkage of the weld in cooling.

Hence, dividing the total stress, or "load in pounds," by the original area of the metal in the plane of rupture, gives a fictitious value of the ultimate strength in "pounds per square inch," the term fictitious having the meaning that the value so obtained differs appreciably from the value for ultimate tensile strength that will be shown for the same plate metal when tested by the present accepted method for determining its tensile strength. It is true that these fictitious values may be used for plotting a quasi elastic curve, each point showing the relation between the elongation and the "load in pounds" which produced it. But the shape of this curve will not be exactly the same as elastic curves determined by other methods.

There is required for our type of test specimen, a correction factor for converting "load in pounds" to "pounds per square inch," and our tests have shown its value is not a constant for the entire range of applied stresses from zero to that required to rupture the specimen. Approximate values of this correction factor for plate metal may be obtained from the preceding tables as follows: from columns 5 and 6 of Table 2, the loads in pounds at yield and at rupture of the specimen may be divided by the initial area of metal between the drilled holes, to give values of the stresses actually carried during each of these determinations. The average for three chosen wedge-tested plate specimens are 35,070 at yield and 74,800 at rupture, in pounds per square inch. These figures are to be compared with the averages of the three tensile tests of steel plate specimens in Table 3, namely 33,800 at yield and 61,750 at rupture.

This comparison of unit stresses in the contracted cross section of wedge specimens with the unit stresses in plates of uniform cross section throughout their entire length, gives us the values of the "correction factor" for these two points on the elastic curve, and results in showing that the apparent strength of the contracted area is 3.6% greater than for the uncontracted area at yield point, and 21% greater at point of rupture.

In an effort to throw some light on the elements governing the variations in the value of the correction factors for tensile tests of plates perforated with special drilled holes, a series of tests were undertaken and some results secured are presented below. Seven specimens $1^1/_2$ inches wide and 16 inches long were cut from a steel plate $^1/_4$ inch in thickness. In six of these, two $^3/_{16}$-inch holes were drilled at midlength on a line at right angles to the length of the specimen. The holes were spaced $1^3/_{16}$ in. on centers, leaving 1 in. of metal between them. Saw-cuts were made through the metal remaining outside the holes. Holes of a different diameter for each of the six specimens were then drilled midway between the two $^3/_{16}$-inch holes.

In the following table, the ultimate tensile strength (Col. 3) in the first six items is the result of dividing the total stress in pounds applied to produce rupture at the contracted section by the initial area of that cross section. The "correction factor" (Col. 4) is determined by dividing the ultimate strengths so found by the ultimate strength determined for the seventh specimen, which was prepared and tested as specified for tension tests of steel plates.

Table 4—Tensile Tests of Steel Plates

Test No.	Diameter of Center Hole	Ultimate Strength at Minimum Cross Sec.	Apparent Increase in Unit Strength
46	0	69,000 lb. per sq. in.	10%
47	$^1/_{16}$ in.	74,400 " " " "	19%
48	$^1/_8$ in.	75,400 " " " "	20%
49	$^3/_{16}$ in.	75,500 " " " "	20%
50	$^1/_4$ in.	76,400 " " " "	22%
51	$^1/_2$ in.	75,300 " " " "	20%
52	No holes*	62,600 " " " "	0

* A standard tensile specimen.

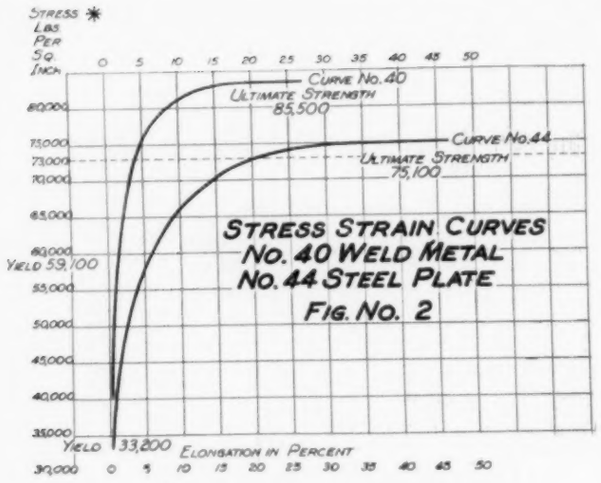
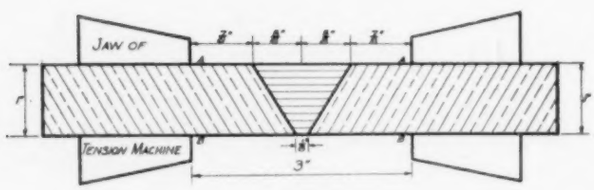

STRESS * LBS PER SQ. INCH
CURVE No. 40
ULTIMATE STRENGTH 85,500
CURVE No. 44
ULTIMATE STRENGTH 75,100
STRESS STRAIN CURVES
NO. 40 WELD METAL
NO. 44 STEEL PLATE
FIG. No. 2
YIELD 59,100
YIELD 33,200
ELONGATION IN PERCENT
* LOAD IN LBS. DIVIDED BY INITIAL AREA OF CROSS-SECTION OF WELD METAL BETWEEN HOLES. ON AXIS OF WELD.

Figure 2 shows a stress-strain diagram comprising two "elastic curves," the upper curve, No. 40, representing weld metal, and the lower curve, No. 44, representing steel plate metal. The elongation percentages and the stresses in pounds per square inch were determined for many points by the "wedge" method previously described. The portions of the curves from zero to the yield point of the steel plate are omitted, being nearly straight lines. The curves indicate that the yield point for the weld metal is reached at a stress of 59,100 pounds, and for the steel at 33,200 pounds. At these stresses the elongations are 1.23% for the weld metal and 5.58% for the steel. In designing welded steel structures, the allowable stresses are determined to a greater extent by the yield of the metals than by the ultimate strength. Hence, the difference indicated above between the metals involved would merit consideration.

The difference in the elongations of the two metals is much greater for higher equal stresses,when the applied stress is about 74,000 pounds for each curve, the elongations are 4.2% for the weld metal and 24.5% for the steel plate. Again considering a steel structure comprising welded joints, the above difference in the elongation of the two metals indicates that an unsymmetrical distribution of the applied stresses would occur at and near the weld, as illustrated in the following case.

Figure 3 shows a single-vee weld in 1-inch plates, beveled to form a 60° V, and separated 1/16 inch at the point of the V. Assume that with both beads ground off flush with the plate, the width of the weld at top of V is 1 1/4 inch and at the point of V is 1/8 inch. Also that the jaws of the tension machine are placed 1 1/2 inch on either side of the center line of the weld. Then if a tension stress of 73,000 pounds per square inch is produced along the top surface of the specimen where the weld metal occupies 1 1/4 inch of the total span between the jaws, and the plate metal occupies the remaining 1 3/4 inch, the total elongation produced is found by referring to the curves on Fig. 2 to be 0.3975 of an inch, of which 0.0475 of an inch occurs in the weld and 0.3500 of an inch in the steel plates. An equal total elongation will necessarily be produced along the lower surface of the specimen which contains the joint of the V. Of the total elongation along the latter surface only 0.0045 of an inch occurs in the 1/8 inch of weld metal, the remaining 0.393 of an inch elongation occurring in the 2 7/8 inch of plate metal. This is a 13.65% elongation for the plate metal.

ASSUMED
TENSILE STRESS ALONG TOP SURFACE A-A = 73,000 LBS. PER SQ. IN. RESULTING ELONGATION IN 1 1/4" OF WELD METAL = .0475". RESULTING ELONGATION IN 1 3/4" OF PLATE METAL = .3500". TOTAL ELONGATION A-A = .3975".



COMPUTED
ELONGATION IN 1/8" OF WELD METAL AT POINT OF V = .0045". ELONGATION IN 2 7/8" OF PLATE METAL = .3930". STRESS REQUIRED ALONG B-B TO PRODUCE TOTAL ELONGATION OF .3975" = 69,500 LBS. PER SQ. IN.

FIG. No. 3

Referring now to curve No. 44 on Fig. 2, we find that 13.65% elongation in the steel plate will be caused by the application of a tensile stress of only 69,500 pounds, which is 3500 pounds less than the 73,000 pounds stress existing along the opposite surface of the specimen according to our first assumption. This analysis of a special case serves to indicate the unequal distribution of stresses that may occur in any welded structure as a result of the marked differences in the ductility of the weld metal as compared with the base metal in a welded joint.

In conclusion, we submit the "wedge" method for measuring accurately, the elongations of the metal in a weld resulting from a series of applied tensile stresses, from zero to the breaking stress.

Welding Research Activities in Germany

ISSUE No. 30, May 1937, of the Proceedings of the Welding Committee of V D I (German Society of Engineers) of which Dr. Burkhardt is Chairman, lists the following research projects recently completed:

No. 74 Effect of Shrinkage Stress on High-Temperature Creep (Federal Materials Testing Bureau)
No. 76 Time Study in Manual Welding (Refa)
No. 77 Ductility Tests on Welded Joints (Professor Matting)
No. 78 Reasons for Poor Weldability of Certain Kinds of Copper (Dr. Söhnchen)
No. 79 Instruction Sheets for Welding Light Metals (Professor Matting) New Research Problems are:
No. 62 (continuation) Fatigue Tests of Stainless Steels (Federal Materials Testing Bureau)
No. 73 Reviews of Current Welding Literature (Dr. Adrian)
No. 80 Formation of Nitrous Oxide in Welding in Confined Space (Federal Chemical Laboratory)
No. 81 Wear Tests of Welded Joints (Professor Behrens)
No. 82 Tests of the Swedish Two-Hole Tensile Specimen for Welds (Dr. Kühnel)
No. 83 Tabulated Data on Strength of Welded Light Alloys (Dr. Adrian)

The Subcommittee on Filler Metal proposes to omit Brinell Hardness requirements from the German Standard D I N 1913, and to change the sulphur and phosphorus limits of all steel filler metal so that the combined sulphur and phosphorus content shall not exceed 0.07%.

Welding Special Ferrous Alloy Castings
A Review of the Literature to November 1, 1936

By W. SPRARAGEN* and G. E. CLAUSSEN†

This Report Is Prepared Under the Auspices of the Literature Subcommittee of the Engineering Foundation Welding Research Committee

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Caution.—The series of reviews on welding cast ferrous materials emphasizes the fact that there are vast degrees of difference in the extent to which the welding of castings differs from the welding of wrought material of the same composition. The extent of these differences varies with the composition under consideration. The special characteristics of castings may cause welding difficulties with one composition, but not with another.

Consequently, it should be remembered that the welding of some of the special ferrous alloys dealt with in the present report is practically the same whether in the cast or wrought form, for example, 14% Mn steel. Only the literature dealing with the cast form is reviewed herein.

SUMMARY

14% Manganese Steel

Technique—Oxyacetylene Welding.—The technique of oxyacetylene welding consists of using a rod of the same composition as the casting, a flame with or without a slight excess of acetylene, and a torch of relatively large capacity. The rod is melted by dipping in the bath, which is kept molten only so long as necessary to melt the edges of the scarves. Neither scarves nor rod should be melted by direct action of the flame. The weld should be heated ½ hour at 1060° C. and water quenched. The casting is preheated dark red to avoid cracks, and borax is used as flux. Oxyacetylene welds made with a rod containing up to 5% nickel in addition to the usual manganese are air-toughening but usually require heat treatment. 18–8 rods avoid cracks and distortion, and may be used without flux with a slightly reducing flame.

Technique—Metal Arc Welding.—The metal arc welding of 14% Mn castings is generally carried out with a rod containing 11—13½ Mn; 0.70—0.90 C; 2½-5Ni. The root spacing must be larger than for mild steel on account of the larger coefficient of thermal expansion of the 14% Mn steel. Heat treatment is unnecessary after welding with Ni-Mn electrodes.

Current values should be low. Beads of each layer should be laid at right angles to the beads of the preceding layer. Sometimes a layer of 18–8 is deposited on the casting first to seal cracks, the Mn steel electrode being used for additional layers.

Welding to Plain Carbon Steel.—In order to weld 14% Mn steel castings to plain carbon steel of any analysis, a Ni-Mn rod should be used for the Mn side, and another rod containing 5% Ni, No Mn and less than 0.20% C. for the plain carbon side.

Physical Properties.—Welds made with Ni-Mn electrodes have about the same properties as the base metal, but these results are not attained with plain 14% Mn rods.

High-Chromium and Chromium-Nickel Alloy Steel Castings

Nickel-rich and other C-Ni (completely austenitic) castings do not require heat treatment after welding, but as Cr and C are increased, especially with oxyacetylene welding, preheating to 600° C. is necessary with cooling as slow as possible. Plain Cr castings are preheated. High Cr, high-carbon wear-resisting castings are preheated red; flux is used to remove oxides, and the weld is heat-treated.

High-Silicon Iron

The oxyacetylene process is used with neutral flame, Duriron rod, and flux of equal parts of borax and sodium bisulfate. The rod is kept in the molten pool, which is very fluid, under a heavy slag covering. The beveled casting is preheated to 1100° F.(600° C.). On account of fluidity, the welding should be done more rapidly than for unalloyed cast iron.

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Welding Special Ferrous Alloy Castings

Introduction

THE present review summarizes available published technical literature on the welding of three main types of special (high-alloy) ferrous castings: 14% Mn steel, otherwise known as Hadfield or Austenitic manganese steel, high Cr steels with and without nickel, and high-silicon iron (15%Si), as well as of several miscellaneous compositions. Some of the materials are welded in both the cast and wrought forms, but in this review only the problems peculiar to the cast form are dealt with.

14% Manganese Steel

Technique—Oxyacetylene Welding

The technique of oxyacetylene welding, according to *Oxy-Acetylene Tips*,[1] consists of using a rod of the same composition as the casting, a flame with a slight excess of acetylene, and a torch of relatively large capacity. The rod is melted by dipping in the bath which is kept molten only so long as necessary to melt the edges of the scarves. Neither scarves nor rod should be melted by direct action of the flame. The weld should be heated $^1/_2$ hour at 1060° C. and water quenched. Other writers are in substantial agreement, Hönisch[2] stating that if the rod is melted dropwise the weld is bound to be of poor quality. Greger[3] mentions a special rod (no details) having good fluidity and giving rise to no sparking. The casting is preheated dark red to avoid cracks, and borax is used as flux. Hall[4,17] states that gas welds must be heat-treated to remove brittleness, unless the Timang (Ni-Mn) rod is used, and deplores the use of anything but a neutral flame. A flame with slight excess of acetylene dissolves oxide on the surface of the puddle and facilitates welding but destroys the strength of the weld. A soft flame (low pressure) is better than a hard flame (high pressure), which tends to make the molten metal bubble. Oxyacetylene welds made with a Timang rod are air-toughening but the oxyacetylene welded casting usually requires heat treatment. Almost the same practice is advocated by W. J. Dawson (private communication April 1937) using Hadmang (Ni-Mn) uncoated rods, except that the flame should have the slightest trace of excess acetylene.

Industrial Gases[5] emphasize that the entire welded part must be totally immersed in water after heating to 1060°C. Preheating and a 90° V joint are recommended by Jones.[6] An anonymous writer[7] finds that 18–8 rods avoid cracks and distortion, and may be used without flux with a slightly reducing flame. S. W. Miller[8] found the changes in composition shown in Table 1 as a result of gas welding.

Table 1—Recovery of Carbon and Manganese in Gas Welding. Miller[8]

	% C	% Mn
Rod	1.18	12.23
Deposited Metal Neutral Flame, Flux	1.04	11.28
" " Reducing " "	1.22	12.23

Technique—Metal Arc Welding

The metal arc welding of 14% Mn castings is generally carried out with a rod containing 11–13$^1/_2$ Mn; 0.70–0.90 C; 2$^1/_2$–3$^1/_2$ Ni, as Payne[9] points out. After depositing not more than 2 inches the bead is hot peened. The root spacing must be larger than for mild steel on account of the larger coefficient of thermal expansion of the 14% Mn steel. There is little magnetic arc blow. Sanderson,[10] whose electrode contains 15 Mn, 0.6–0.8 C, 3–5 Ni, and is as small in diameter as possible, states that preheating is desirable. The weld, after being heated $^1/_2$ hr at 680° C., should be quenched in lukewarm water. A 90° V joint is best. Sanderson's heat treatment is not general practice. Quinn[11] also recommends an electrode containing 4$^1/_2$–5% Ni and states that peening should not be excessive. The current values in Table 2 are recommended by Jennings.[48] The voltage is 20 to 25. He and Hall[17] advocate the crescent weave with beads $^1/_2$ to $^3/_4$ in. wide. Beads of each layer should be laid at right angles to the beads of the preceding layer. Peening should start at the crater and proceed toward the start of the bead. Jennings makes no distinction between the welding of cast and rolled material. Current values for the "Manganweld" electrode[49] (Ni-Mn-Mo shielded arc, reversed polarity) are given in Table 3. The hardness of deposited metal is 5 to 10 C Rockwell, which is increased to 46 to 50 C by cold work. The casting only, not the weld, may be cooled with water periodically to prevent distortion and possible cracking of casting.

Table 2—Current for Ni-Mn Electrodes. Jennings[48] and Hall[17]

Electrode Diameter, Inch (4% Ni)	Current, Amps.
$^1/_8$	75 to 100
$^1/_4$	175 to 225

Table 3—Current for Ni-Mn-Mo Electrodes. Lincoln Elec. Co.[49]

Electrode Diameter, Inch (Manganweld)	Current, Amps.
$^1/_8$	50 to 90
$^1/_4$	170 to 225

The "Hadmang" electrode (Ni-Mn), according to W. J. Dawson (private communication April 1937), is supplied in two forms: coated and bare. Recommended currents are given in Table 4.

Table 4—Current for Ni-Mn Electrodes (W. J. Dawson)

Electrode Diameter, Inch	Current, Amps.	
	Bare	Coated
$^1/_8$	75–100	50–90
$^1/_4$	175–225	160–210

The bare electrode can be used only with D.C. (electrode positive) and in still air; the coated may be used with A.C. or D.C., in windy situations, and where contact between electrode and deposited metal may occur. However, the bare electrode provides a weld metal which is completely immune from surface cracks and is definitely recommended for welds in castings which must withstand severe operating conditions. The beads should be $^3/_8$ to $^3/_4$ inch wide, depending on gage of electrode, and should be about 3 inches long, never exceeding the deposit obtainable from one electrode.

The loss of Mn and C increases as the arc is lengthened, according to Sheldon.[12] An anonymous writer[13] (1931) used a $^3/_{16}$-inch electrode, 150-200 amps, and found that reversed polarity prevented boiling. He recommends that the metal be deposited on the casting in the form of

short pads, not beads, by giving an oscillating motion to the electrode. The weld is peened hot, water cooled and peened again cold.

Regé[53] gives the following composition for a coated electrode containing 13 Mn, 1.3 C, 0.04 Si, for welding 14% Mn steel: 13% talc or asbestos; 33% pumice, 13% pyrolusite (mixture of iron and manganese oxides); 32% sodium silicate (36° Be); 2% graphite; 5% ferro-manganese (75% Mn); 2% ferro-silicon (95% Si.).

The technique of using the coated $3^1/_2$% Ni electrode is also discussed by Hurme.[14] Peening of each short bead, which is essential to relieve shrinkage stresses and to shape the weld, is done with a 3 to 4 lb. hammer. Low current and short arc are necessary. Sometimes a layer of 18–8 is deposited on the casting first to seal cracks, the Mn steel electrode being used for additional layers. It is usually considered at present that better properties are obtained with a Ni-Mn electrode than with 18–8. Hatfield[15] favors the use of a layer of austenitic Cr-Ni steel interposed between casting and deposited Mn steel and states further that the $3^1/_2$ Ni-Mn electrode should be deposited in light runs and at low temperatures. Heat treatment of the weld is desirable. On the other hand, Hall,[16] who gives a good description of welding methods, states that the Ni-Mn weld does not require a water quench; the air cool after welding is adequate. The Electric Heating and Welding Committee of the A. R. E. A.[46] state that if the carbon content of the Ni-Mn electrode (3 to 5Ni, 11 to 15Mn) is as high as 0.90 to 1.15 per cent, the bead is likely to crack before it can be peened, particularly in cold weather. A coating is necessary for A.C., but not for D.C. (reversed polarity) Hall[17] states that a plain 14% Mn rod causes cracking and spalling and recommends the Timang (U. S. Patent 1,732,202) composition: 12–15 Mn, 0.6–0.8 C, 3 Ni. The casting usually contains 10–14 Mn, 1.0–1.4 C, 0.3–1.0 Si, 0.04–0.10 P, 0.01–0.03 S. The currents recommended by the A. R. E. A. and Hall are the same as those given by Jennings above. The general rule is to keep the current as low as possible consistent with good penetration. The best arc length, Hall states, is obtained by first drawing a long arc, then shortening it until the shortest stable arc is obtained.

Lebrun[18] illustrates several studded and grooved joints said to be useful for welding 14% Mn castings. In 1916 Armstrong[19] described a slag coated 13% Mn electrode which yielded a deposit containing 9% Mn.

Welding to Plain Carbon Steel

In order to weld 14% Mn steel castings to plain carbon steel of any analysis, a Ni-Mn rod should be used for the Mn side, according to Hall,[20] and another rod containing 5% Ni and less than 0.20% C for the plain carbon side (J. H. Hall, private communication, April 1937). The method is patented (U. S. Patent No. 2,060,765). The low-carbon nickel steel rod used for the plain carbon side does not lead to the difficulty, mentioned later in this section, of contaminating the plain carbon steel with manganese. Hall has found that the use of two different rods is absolutely necessary if the carbon content of the plain carbon steel is high; two rods are not necessary for softer grades of steel.

Rapatz[21] and Lincoln Electric Co.,[49] however, state that a high-carbon Ni-Mn shoud not be used for such welding; a heavy-coated low-carbon austenitic rod should be used, otherwise at some point in the junction zone the casting will contain 4% Mn, a very brittle composition.

Flame Cutting

Examples of flame cutting are given by Davis[22] and Coyle,[23] and an anonymous writer[24] considers that the 14% Mn castings are easily flame cut. Holler and Schneider[25] give the data in Table 5 on the rate of flame cutting 14% Mn steel castings without preheat.

Table 5—Flame Cutting 14% Mn Castings. Holler and Schneider[25]

Thickness, Inch	Cutting Velocity Feet/Hr	Breadth of Heat Affected Zone, Inch
0.4	20	0–0.1
1.0	15	0.1–0.2
2.0	11	0.2–0.3
4.0	9	0.3–0.5

Metallurgy

The metallurgical problems have been concerned mainly with the brittleness of the junction zone, the cause of which was pointed out by Hall.[16,17,20] Failure of welds in plain 14% Mn castings is due to brittleness, (1) in the zone heated between 425° C. and the melting point and cooled slowly, or between 425 and about 800° C. even if water quenched (the embrittlement is due to precipitation of carbides in the as-cast or heat-treated casting); (2) in the zone (burnt zone) brought to temperatures above the solidus but below the liquidus. Flux and inert gases were observed to be without effect, and heat treatment can only cure conditions in zone 1. Hall's excellent micrographs clearly show masses of embrittling material in the burnt zone. The embrittling material is to some extent intercrystalline and presumably has a higher content of carbon and manganese than base metal. Both factors are embrittling in tendency and almost impossible to change by feasible heat treatment. Hall found that the burnt zone in welded castings could be made less harmful by using a low-carbon rod, or by adding 3 to 5% Ni. The carbon content of the burnt zone is thus diluted and the zone is strengthened by impregnation with nickel. Semi-fusion close to the weld is largely prevented close to the weld by using the low-carbon, Ni-Mn rod. Whereas gas welds with the Ni-Mn rod almost always require heat treatment to toughen the broad heat-affected zone, arc welds do not.

Although the investigations by Hall have adequately shown the nature of, and cure for, difficulties in the junction zone of welds in 14% Mn steel castings, it must be said that the explanation of the observed facts is not so clear. It seems that the "perplexing" microstructural peculiarities observed by Miller[26] are now cleared up and the investigation of welds in 14% Mn castings is ready for investigations connecting observed phenomena with studies of critical points and cooling velocities, and of the changes of composition and melting point in the junction zone using Ni-Mn or 18–8 filler rods. Hurme[14] and Quinn[11] state that Ni retains the carbides in solid solution in the weld metal and heat-affected zones of the casting even during slow cooling. Miller[26] showed that in oxy-acetylene welding with a neutral flame without flux, the weld was worthless, containing large quantities of entrapped oxides (probably manganese oxide). Clarke[27] considered the coarse columnar structure of the deposited metal on castings as a distinct disadvantage, and experienced difficulties on account of the high coefficient of thermal expansion of 14% Mn steel.

Physical Properties

According to Hall (reference 28 and private communication, April 1937; the results given in reference 28 are low and out of date), the tensile properties determined on 16 cast 14% Mn steel bars ($^3/_4$ inch diameter) welded with Ni-Mn electrodes were:

tensile strength maximum 71,400 psi
tensile strength minimum 40,000 psi
tensile strength average 55,900 psi

The elongation in 2 inches was 8 to 9% average. The results of 189 bend tests of $^3/_4$ inch square cast 14% Mn steel welded with Ni-Mn electrode were:

bend angle maximum 106°
bend angle minimum 30°
bend angle average 65°

Twist specimens of $^3/_4$-inch square bars of unwelded cast 14% Mn steel broke at $2^3/_4$ and $3^1/_4$ complete twists. Bars welded with Ni-Mn electrodes twisted off at $1^1/_2$ and $1^3/_4$ turns.

The tensile and bend properties of cast 14% Mn steel welded with 18–8 electrodes are inferior to those obtained with Ni-Mn electrodes, as shown in Table 6 (averages of 16 specimens)

Table 6—Properties of Welds in Cast 14% Mn Steel Made with 18–8 Electrodes (Hall)

	Max.	Min.	Average
Bend Angle degrees ($^3/_4$-inch sq. bar)	30	9	16
Tensile Strength, psi	38,700	18,100	28,400
Elongation, % in 2 inches	...	...	3

Butt welds in cast 14% Mn steel $^1/_4$ to $^3/_8$ inch thick, 2 inch wide using a Ni-Mn electrode, according to W. J. Dawson (private communication April 1937), develop a cold bend angle of 80 to 90° over a former 2 inch diam. without cracking. The cold bend test of all-weld-metal (Ni-Mn electrode) $^1/_2 \times {}^7/_{16}$ inch section gives 90° without cracking; the tensile strength is 90,000 psi, elongation $12^1/_2$% on 2 inches, $18^3/_4$% on 1 inch. The tensile specimens were 0.445 × 0.475 inch. None of the specimens quoted by Dawson was heat-treated after welding.

The producers of Ni-Mn electrodes state that the deposited metal has all the essential properties of standard, heat-treated manganese steel.

Impact tests by Roux[51] in 1926 on cast bars (12.25 Mn, 1.25 C), 1.2 x 1.2 x 8 inches long) heat-treated and welded (150–180 amps.) with an electrode containing 15.4 Mn, 1.38 C, were not encouraging.

Physical Constants

Physical constants of sound, fine-grained cast 11–14% Mn steel. (American Manganese Steel Co and Hall[52])
Density (20° C.) 7.91
Melting Point reported by different investigators ranges from 1350 to 1420° C. (2450 to 2590° F.).
Thermal Conductivity 2.31 (copper = 100) (about $^1/_7$ that of plain carbon steel); 0.027 C. G. S. units(0-100° C.).
Electric Resistance: 66 microhms per cm. cube 20° C. (about 4 times that of mild steel)
Specific heat (20° C.): 0.112 to 0.145 (different investigators)
Shrinkage: $^5/_{16}$ inch per foot (normal foundry allowance)
Coefficient of thermal expansion (0.–100° C.) 0.000018 per degree C. (25 to 100% greater than plain carbon steel).

Physical Properties of Cast 14% Mn Steel

	Cast to Size	Ground from Coupon
Tensile Strength, psi	80,000–110,000	118,000
Proportional Limit, psi	...	42,900
Elongation, % in 2 inches	15–35	44.1
Reduction of Area, %	15–35	39.0
Fatigue Limit, psi (H. F. Moore rotating bend machine)	...	39,000

Physical Properties of Cast Ni-Mn Steel (13–15 Mn, 3–5 Ni) Air Cooled from 1650–1850° F. (900–1000° C.). Hall[28]

	Unmachined	Finish Ground
Tensile Strength, psi	97,000	105,500
Proportional Limit, psi	...	27,500
Elongation, % in 2 inches	30.0	56.9
Reduction of Area, %	23.2	48.3
Brinell Hardness	170	
Melting Range	2410–2500° F. (1320–1370° C.)	
Coefficient of Thermal Expansion	0.0000187 (20–100° C.)	
" " " "	0.0000222 (20–500° C.)	

High-Chromium and Chromium-Nickel Alloy Steel Castings

Welding

The Cast Metals Handbook[29] (1935) supplies Table 7.

Table 7—Welding Corrosion Resisting Steel Castings. Cast Metals Handbook[29]

Composition Cr	C	Si	Ni	Remarks
12	0.10	1.0	...	Welds should be normalized
19	0.12	1.0	...	Do not weld except in very thin sections
28	0.50	1.0	...	Cool slowly to 600° C., then rapidly
29	0.30	1.5	9.0	Heat treat after welding
18	0.06–0.15	...	8	" " " "

Nickel-rich and other C-Ni (completely austenitic) castings do not require heat treatment after welding. Leitner[30] states that castings containing 14 Cr, 0.1 C rarely crack in welding, but as Cr and C are increased, especially with oxyacetylene welding, preheating to 600° C. is necessary with cooling as slow as possible.

It is, however, common practice in this country to preheat 14% Cr, 0.1% C castings before welding. Leitner also states that castings containing 20 to 30 Cr (preheat 600° C.) should be heated to 700° C. after welding and air cooled. Castings of the 18–8 type are especially liable to cracking. Dawson[31] believes that filler rod should have the same composition as casting, at least up to 2% C. Plain Cr castings are preheated; Cr-Ni are not. High Cr, high-carbon wear-resisting castings are preheated red, according to Jones;[32] flux is used to remove oxides, and the weld is heat treated.

In oxyacetylene welding cast alloys containing up to 28 Cr or up to 40 Ni and 20 Cr (carbon 0.5 to 2.75% for straight Cr, but 0.50% max. for Cr-Ni), according to Miller,[33] the casting should be preheated dull red, and the rods should have the same composition as base metal. Excess acetylene is permissible if the castings are high in carbon. The casting should always be annealed after welding.

The areas adjacent to welds in stainless steel castings, according to Hodge[34] must be free from red-short cracks, shrinkage cavities, or other porosity, otherwise contraction stresses will cause local cracks.

The welding of Cr-Ni steel castings is described by Thum.[35] After the defect has been cleanly cut to the bottom, the casting is preheated. After welding, heat treatment is necessary. Heat-resisting steel castings can be welded without trouble above 500° F. Castings containing 29 Cr-9 Ni and 15 Cr-35 Ni are easily welded with covered electrodes; heat treat to suit. Wilcox[36] reports that welds in castings containing 29Cr-9Ni, after being boiled 72 hrs. in acid Cu SO_4 solution, were bent 180° without cracking (no details).

There is no difficulty, according to H. Harris (private communication, March 1937), in welding cast tubes of

heat-resisting alloy (20-22 Cr, 8-10 Ni, 0.3-0.6 C, with additions of silicon and aluminum) with low-carbon coated electrodes of similar analysis but without aluminum. In laboratory tests the tubes failed in cast metal remote from the weld. Service results at temperatures up to 1000° C. in boiler furnace gases showed no trace of weld deterioration.

Great difficulty was experienced by Flinterman[47] in welding cracks in castings containing 15 Cr, 35 Ni, 0.45–0.65 C, 1.2–1.4 Si, 0.6–0.8 Mn, but simple castings can be welded together by oxyacetylene or D.C. metal arc processes, using filler rods of the same composition.

Flame Cutting

The flame cutting of castings containing 4 to 7 Cr is described in detail by Priestley[37] The casting should be heated to, but not higher than, 900° F. (480° C.). If the casting contains Ti or Cb most of the difficulties due to hardening are eliminated. Higher Cr and Cr-Ni steel risers are cut by the flux method, holding an iron rod in the cut. The casting should contain a stabilizing element to prevent subsequent intercrystalline corrosion.

The flame cutting of risers on 18–8 castings is also described by Aitchison[38] The tip should be one size larger than for plain carbon steel and should have 6 preheating jets rather than 4. Oxygen pressure should be increased 15 to 20%. The surface to be cut must be free from sand or dirt and preheated to a dark red by means of another torch. The neutral flame is held horizontal and the cut is made from top to bottom, the burner being held 2 inches from the casting and moved back and forth, not sideways, in the line of the cut. The time of cutting is longer, the spatter on the torch side greater, the cut 30% broader, and the edges rougher than for mild steel.

High-Silicon Iron

The welding of high-silicon, acid-resisting castings (Duriron, Antac iron, etc., 14.5 Si, 0.5-1.0 C) is described by Critchett[39] and several other writers[40] who are in substantial agreement. The oxyacetylene process is used with neutral flame, Duriron rod, and flux of equal parts of borax and sodium bisulfate. The rod is kept in the molten pool, which is very fluid, under a heavy slag covering. The beveled casting is preheated to 1100° F. (600° C.). On account of high fluidity, the welding should be done more rapidly than for unalloyed cast iron. Casting cracks tend to open during or after welding. According to Hatfield,[15] the welds do not have very good physical properties, although circumstances justify their use. A writer[50] in 1918 was successful in oxyacetylene welding preheated Duriron with gray cast iron rods.

Miscellaneous Alloy Castings

Nicrosilal (1.8 C, 6.0 Si, 18 Ni, 2 Cr, 1 Mn) and Ni-Resist, according to Pearce,[41] are welded with greater certainty than gray cast iron. He presents a brief micrographic study of Nicrosilal oxyacetylene welded with Nicrosilal rod, and also with Monel Metal rod. Müller and Hanel[42] mention the welding of Ni-Resist with coated Monel electrodes.

According to Vanick and Merica,[43] Ni-Resist can be metal-arc or oxyacetylene welded with a rod of the same composition and cast-iron flux without developing hard ridges. Technique, preheating practice, types of joint, puddling and cooling after welding Ni-Resist (2.90 C, 1.50 Si, 1.10 Mn, 14.0 Ni, 6.0 Cu, 2.0 Cr) are the same as for unalloyed gray cast iron, according to the International Nickel Company (private communication, Jan. 1937). A coated Monel Metal rod is satisfactory for arc welding; Monel may also be used for gas welding.

An anonymous writer[44] states that acid-resistant, high Cr cast iron must be preheated, the filler rod having the same composition as the casting. A rich, excess acetylene flame is used with flux. Müller,[45] who deals briefly with the welding of Hastelloy, states that castings of the C (60 Ni, 17 Mo, 15 Cr, 6 Fe) and D (85 Ni, 10 Si, 3 Cu) grades require preheating for welding.

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Welding Malleable Cast Iron
A Review of the Literature to December 1, 1936

By W. SPRARAGEN* and G. E. CLAUSSEN†

This Report Is Prepared Under the Auspices of the Literature Subcommittee of the Engineering Foundation Welding Research Committee

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Caution.—The review emphasizes the difference in welding characteristics between black-heart and white-heart malleable cast iron. Almost all malleable cast iron in this country is black heart. Statements in the review bearing on white-heart should not be considered to apply also to black-heart.

SUMMARY

Introduction

The malleable cast iron in common use in this country is "black-heart" malleable cast iron (0.6–1.1 Si, up to 0.4 Mn, under 0.20 P, 0.06–0.15 S). Since the physical properties are dependent on certain heat treatments (malleableizing) which it undergoes, the heat of fusion welding, as may be expected, will permanently change the structure and physical properties unless the temperature is kept low (750° C. as will be noted later).

Methods of Welding

Bronze-welding has therefore become the usual method of repair. The method is the same as for gray cast iron. The edges to be welded are beveled to 90° and cleaned. The scarfs should not be ground; grinding is said to bring graphite to the surface and to make tinning difficult. The casting is then preheated to a black heat (locally for small castings) and bronze-welded using a tip one size larger than for similar work on steel, and an oxidizing flame. Flux is used liberally. The rod may contain 59 Cu, 40 Zn, 1 Sn or 1 Fe plus some Mn; lead is bad. Other rods such as manganese bronze and Cu-Zn-Si high strength brass have also been found to be successful.

If the casting may be re-annealed, that is, malleableized after welding, both arc and gas welding may be used.

The best procedure for black-heart is to use oxyacetylene welding on the preheated casting whether hard or annealed, employing a white cast iron filler rod. Re-annealing is necessary and the weld then has a structure similar to the casting, but finer grained. For (decarburized) sections up to nearly 1/4 inch thick the castings can be welded like cast steel, using borax and mild steel filler rod. Above 1/4 inch the same procedure as for gray cast iron is used. Plenty of borax and a high-silicon rod are employed. After being welded, the castings are heated rapidly to 760 to 800° C., held 1/2 to 1 hour, and cooled slowly in sand. Up to 0.3 inch thick white-heart castings need not be re-annealed.

Black-heart may be welded with steel electrodes in the same way as gray cast iron, but annealing after welding is essential. Special coated electrodes using small diameter electrode and low current are recommended.

It has been suggested that white-heart castings may be annealed 30 min. at 900–950° C.; black-heart 2 hours at 900° C. (packed in sand) for best results after welding, except for simple small or thin castings for which annealing is unnecessary. Double annealing is of no value. These treatments do not appear, however, to have any successful practical application and are not recommended by the reviewers.

Monel Metal electrodes or other nickel-alloy rods are recommended to weld small defects and build up unfinished surfaces when the malleable casting must be machined after welding.

White castings may be resistance welded prior to malleableizing. White cast iron is easily oxyacetylene welded using white cast-iron rods of the same composition as the casting. Bevels are ground or flame cut and the casting slightly preheated.

Metal arc and oxyacetylene welding is used: (1) to repair cracked white castings; (2) to fill in surface imperfections of annealed castings (a more common practice than No. 1). In both cases welding is followed by the usual annealing cycle, and the welding rod is usually white cast iron of the same composition as the original casting. The welds are almost indistinguishable from base metal after malleableizing. Alloy malleable cast iron is said to behave substantially the same as unalloyed in welding, although some analyses may give difficulty.

Metallurgy

The weldability of white- and black-heart malleable cast iron by metal arc and resistance butt processes increases with decrease of total carbon, silicon, phosphorus, sulphur, gases and inclusions, and increases with manganese up to 0.5%, weldability being measured by physical properties.

Micrographs of specimens of white-heart and black-heart heated to fusion on one end and fitted with thermocouples along the heat gradient show that the hard zone forms at 1045° C. in white-heart and at 950° C. in black-heart. Micrographs also show the impossibility of avoiding the hard zone in malleable cast-iron welds, except bronze welds, short of re-annealing.

The maximum temperature to which black-heart malleable may be heated by the torch in bronze-welding is 775° C., above which hard layer is produced.

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Replies to the questionnaire of the Fundamental Research Committee showed that 700 to 760° C. is regarded in this country as the critical point or maximum temperature to which a malleable casting may be momentarily heated without embrittlement by pearlite formation.

Physical Properties

The tensile strength and elongation of good arc, gas and resistance welds in malleable cast iron which have been malleabilized after welding are approximately 90% of those of the original malleable cast iron.

Welding Malleable Cast Iron

Introduction

THE malleable cast iron in common use in this country is "black-heart" malleable cast iron (0.6–0.1 Si, up to 0.4 Mn, under 0.20 P, 0.06–0.15 S).

Another malleable cast iron, known as "white-heart," is used to some extent in foreign countries. It is entirely different from "black-heart" and contains approximately up to 1.0% C., 0.4% combined C., 0.5–1.0 Si, 0.1–0.4 Mn. In this review all references to "malleable cast iron" are to the black-heart variety unless otherwise stated.

Black-heart malleable is an example of a material whose structure and mechanical properties are permanently changed by ordinary fusion welding. A characteristic of malleable cast iron is its decarburized skin, usually not more than a few hundredths of an inch thick, which consists of practically carbon-free iron, and is removed by machining. Although malleable cast iron has a much lower carbon content than gray cast iron, it is much more difficult to weld from the standpoint of obtaining welds having the same properties as base metal. Nothing appears to have been published about the welding of the newer alloy malleable cast irons. Comparison of the properties of black-heart malleable cast iron with gray cast iron and cast steel is made in "Welding Cast Iron" Appendix B, published as a supplement to the March 1937 issue of THE WELDING JOURNAL.

Methods of Welding

It is the experience of many welders[1] that it is either impossible to weld malleable cast iron or too difficult to weld it profitably. Bronze-welding has therefore become the usual method of repair. The method, according to *Gas Industry*[2] is the same as for gray cast iron. The edges to be welded are beveled to 90° and cleaned. The scarfs should not be ground; grinding is said to bring graphite to the surface and to make tinning difficult. The casting is then preheated to a black heat (locally for small castings) and bronze-welded using a tip one size larger than for similar work on steel and an oxidizing flame. Flux is used liberally. The rod may contain 59 Cu, 40 Zn, 1 Sn or 1 Fe plus some Mn; lead is bad. Busquet[1] recommends manganese bronze and Séférian[3] a Cu-Zn-Si high-strength brass (m.p. 880° C.). Knez[4] and a writer[5] in Foundry Trade Journal state that 800° C. is the upper limit for melting point of bronze for malleable cast iron, but von Rössler[1] and Becker[6] place the upper limit at 850 to 900° C. The International Acetylene Association[7] however, have found that high-nickel rods (composition not given) may be used, notwithstanding von Rössler's statement that the melting point of monel metal is too high. According to Walker,[32] loose carbon on the surface of black-heart may be consumed by licking the surface of the metal with the white cone of the flame before tinning. He recommends a tip one size smaller than for gray cast iron. The important thing to bear in mind in the bronze-welding of malleable cast iron is to keep the heat low.

Both gas and arc welding may also be used. According to Schwartz,[8] the best procedure for black-heart is to use oxyacetylene welding on the preheated casting whether hard or annealed, employing a white cast-iron filler rod. Re-annealing is necessary and the weld then has a structure similar to the casting but finer grained. Schwartz[9] claimed that such welds could not be detected. If a steel filler rod is used there is excessive oxidation, unsatisfactory fluidity, and hard zones in the annealed weld. If a high-silicon gray cast-iron rod is used the junction zone is steely. The oxyacetylene welding of white-heart and black-heart is discussed by Tilemann.[10] For sections up to nearly 1/4 inch thick (Tilemann's castings were deeply decarburized) the castings could be welded like cast steel, using borax and mild steel filler rod. Above 1/4 inch the same procedure as for gray cast iron was used. Plenty of borax and a high-silicon rod were employed. After being welded, the castings were heated rapidly to 760 to 800° C., held 1/2 to 1 hour, and cooled slowly in sand. Up to 0.3 inch thick white-heart castings need not be re-annealed. It should be noted that in his more recent report,[11] Tilemann does not include gas welding among the processes applicable to malleable cast iron.

Knez[4] has found no difficulty in gas welding thin sections of well-decarburized white-heart malleable (0.1 to 0.2 C) using low carbon steel filler rod. The weld requires no heat treatment. Soft, machinable oxyacetylene welds in white-heart malleable cast iron may be made with a mild steel rod without flux, according to Raabe,[12] who states black heart is gas welded like gray cast iron. A writer[13] in 1918 also found that wrought iron rods could not be used for oxyacetylene welding black-heart malleable. A good non-ferrous rod to secure color match is Ambrac B (F. E. Rogers, private communication, April 1937) containing 65% Cu, 30% Ni, 5% Zn.

Von Rössler[1] also states that cracks usually occur when black-heart is gas welded and that the hard zone is much thinner with arc welding (mild steel electrode) than with gas. Schwartz,[14] who used white or malleable cast-iron electrodes with re-annealing, also noted that arc welding caused as little change as possible. The Lincoln Electric Company[15] and Mawson[16] have found that black-heart may be welded with steel electrodes in the same way as gray cast iron, but annealing after welding is essential. Mawson recommends the carbon arc for burning out sandy patches before cold arc welding. Spence[17] came to the same conclusion but used a cast-iron electrode (positive). Since it is easy to obtain penetration on the decarburized surface of black-heart castings, but not in the graphitic core, Sarazin[18] recommends that the fracture should not be chipped but that straps should be welded across it. The casting may be preheated to 700° C. to secure better penetration, but if heavy beads are deposited there is incipient melting below the decarburized surface and, consequently, formation of cavities.

Advantage was early taken[19] of the decarburized layer on malleable castings to use mild steel electrodes without producing unmachinable areas. Short-cycle malleable has a very much thiner decarburized layer than malleable made by older methods. Diehl[20] did not obtain satisfactory results by depositing alternate layers of steel and cast iron in welding malleable.

Detailed instructions for arc welding are given by Tilemann.[11] Covered or dipped electrodes, (0.16 inch

diam.) reversed polarity and preferably asbestos-wound, containing 0.2 C, 0.06 Si, 1.1 Mn, 0.020 P, 0.03 S (±10% tolerance) with a coating of the composition shown in Table 1 are suitable.

Table 1—Coating Used by Tilemann

40–50% graphite	10–20% CaO	3.0–4.0% Alkalies
1.0–5% Fe_2O_3	0.6–3.5% MnO	30–50% loss on ignition
0.0% P_2O_5	1.2–2.5% Al_2O_3	(no preheat was necessary)
20–25% SiO_2	0.8–2.0% MgO	

The coating produces a weld metal containing 0.9% Si. For all sections (0.15 to 0.8 inch thick) the amperage is 100 amps., 25 volts, 60 volts open circuit. White-heart castings may be annealed 30 min. at 900–950° C.; black-heart 2 hours at 900° C. (packed in sand) for best results after welding, except for simple small or thin castings for which annealing is unnecessary. Double annealing is of no value. Tilemann did not consider obtaining complete graphitization below the critical point in any of his heat treatments.

Monel Metal electrodes are recommended by E. H. Horstkotte (private communication March 1937) to weld small defects and build up unfinished surfaces when the malleable casting must be machined after welding.

The resistance butt welding of malleable cast iron has also been described by Tilemann.[11] For sections up to 0.6 inch thick, the resistance butt process is successful with white-heart but a failure with black-heart. The best grip length for white-heart is 4.6 d (d = diameter of cast iron bar); the distance of upset is 0.08 to 0.16 inch. The faces to be welded must be smooth. Resistance butt welds are improved by light hammering followed by annealing in the machine. All welds must be annealed, and reinforcement, for some reason, must be removed before annealing. White iron castings may be resistance welded prior to malleableizing, according to Touceda (private communication, Jan. 1937). As Corey[21] pointed out, white cast iron is easily oxyacetylene welded using white cast-iron rods of the same composition as the casting. Bevels are ground or flame cut and the casting slightly preheated. It is surprising that little difficulty with shrinkage cracks has been encountered in white cast-iron welding.

The replies to a questionnaire sent by the Fundamental Research Committee to a number of manufacturers and users of malleable castings showed that metal arc and oxyacetylene processes are used:

(1) to repair cracked white castings

(2) to fill in surface imperfections of annealed castings (a more common practice than No. 1).

In both cases welding is followed by the usual annealing cycle, and the welding rod is usually white cast iron of the same composition as the original casting. The welds are almost indistinguishable from base metal after malleableizing. According to E. H. Horstkotte (private communication, March 1937), any casting in which a large area has been affected by welding is usually heat-treated:

heat to 850° C.
cool to 740° C., hold 2 hours
cool to 730° C., hold 2 hours
cool to 720° C., hold 2 hours
cool in furnace.

The metal-arc process appears to be slightly favored over the oxyacetylene perhaps because the heat effect is more general in the latter process. Alloy malleable cast iron is said to behave substantially the same as unalloyed in welding, although some analyses may give difficulty. The filling-in of defects in the surface of malleable castings by the rapid application of a soft iron electrode is also practiced to some extent.

The welding of steel parts to malleable castings is practiced, but there is little information on the subject. Miller[22] describes the bronze-welding of steel to malleable castings. An anonymous writer[23] states that steel and malleable should be preheated bright red. Flux is sprinkled on the edges, and surrounding areas are plated with a $^1/_4$-inch Tobin rod. The weld itself is then made. According to E. H. Horstkotte (private communication, March 1937) 18–8 rod is used to weld steel brackets to malleable castings if no machining is to be done after welding.

Metallurgy

According to Tilemann,[11] the weldability of white-heart and black-heart malleable cast iron by metal arc and resistance butt processes increases with decrease of total carbon, silicon, phosphorus, sulphur, gases and inclusions, and increases with manganese up to 0.5%, weldability being measured by physical properties. Notwithstanding, Tilemann also believes that high silicon and carbon may be an aid to weldability from the standpoint of deoxidation. Weldability by either process was practically unaffected by wall thickness. Temper graphite was formed in the weld only if the weld were not overheated. Less uniform penetration of deposited metal was observed in malleable castings than in the original white cast iron. Table 2 shows the recovery of elements in welds made with the electrode recommended by Tilemann. The coating contained 48.40% graphite, 21.09% SiO_2, 17.30% CaO.

Table 2—Recovery of Elements in Arc Welding Malleable Cast Iron. Tilemann

Material	C	Si	Mn	P	S
A. W. electrode (0.16 inch diam.)	0.19	0.06	1.08	0.020	0.030
All-weld-metal	3.20	0.93	0.27	0.027	0.020
White-heart cast iron unwelded	2.22	0.75	0.19	0.09	0.20
White-heart cast iron weld	1.40	0.89	0.22	0.043	0.123
Black-heart cast iron unwelded	2.38	0.78	0.43	0.066	0.050
Black-heart cast iron weld	1.55	0.84	0.50	0.041	0.080

Practically the only other metallurgical factor that has been investigated is the maximum temperature to which malleable cast iron may be heated without formation of hard zones. Micrographs of specimens of white-heart and black-heart heated to fusion on one end and fitted with thermocouples along the heat gradient show, according to von Rössler,[1] that the hard zone forms at 1045° C. in white-heart and at 950° C. in black-heart. His micrographs also showed the impossibility of avoiding the hard zone in malleable cast-iron welds, except bronze welds, short of re-annealing. Schwartz,[8] however, believed that his micrographs (not well reproduced) proved the feasibility of good results from welds. That good results can be expected from bronze-welding is confirmed by the microscopic study reported by Gerbeaux.[24] Contrary to von Rössler, Pearce[25] considers that graphite in malleable cast iron goes into solution at the A_1 point (not above 760° C.). The cooling rate is almost always too rapid to prevent the formation of cementite. According to Afanassyev and Sorokovski,[26] graphite in black-heart commences to dissolve at 800° C. during welding. The broad hard layer that is formed is unaccompanied by blow-holes, and, furthermore, over-

heating is not troublesome because inelastic accommodation to stress occurs in the soft ductile metal beyond the hard zones. L'Air Liquide[27] consider that the maximum temperature to which black-heart malleable may be heated by the torch in bronze-welding is 775° C., above which hard layer is produced. Tilemann[10] observed that the graphitic interior metal in black-heart has a much lower melting point than the decarburized surface; blowholes or cavities are therefore produced.

Replies to the questionnaire of the Fundamental Research Committee showed that 700 to 760° C. is regarded in this country as the critical point or maximum temperature to which a malleable casting may be momentarily heated without embrittlement by pearlite formation.

The following information on the effect of cooling rate on critical points in black-heart malleable cast iron is supplied by H. A. Schwartz (private communication, Feb. 1937). "Malleable iron when cooled from above its A_1 point by oil or water quenching becomes martensitic. It is, therefore, reasonably certain that the critical point with such rapid rates of cooling has been depressed to below 300° C. In determining critical points at heating and cooling rates of about 15 to 20° C. a minute, the difference between the ascending and descending A_1 points of the stable (iron-graphite) system with 1% Si may be about 50° C., decreasing somewhat as the silicon is increased. With a cooling rate of about 45° C. an hour, on the other hand, metallographic evidence points to a depression of the A_1 point of 50 or 60° C. At 15° C. per hour the depression will not be more than 10° C. less. At first glance it seems surprising that these depressions are greater than the lag between the ascending and descending stable A_1 points referred to above. However, metallographic evidence of the entire absence of solid solution at the quenching temperature also implies absence of the metastable solid solution, which adds about 10° C. to the lower end of the depression. The formation of graphite from solid solution as it passes through the A_1 point is much slower than the formation of cementite, so that a fully annealed malleable becomes pearlitic on anything but quite slow cooling through the critical."

The flame hardening of malleable cast iron has been mentioned as advantageous on several occasions. Thum[30] cites an instance in which the Shore hardness of malleable was raised locally from 15 to 50, the torch being applied for 2 to 8 seconds before quenching.

The flame hardened surface of an incompletely malleableized cast iron 7/8 inch square containing 1.78% graphitic carbon, 0.93% combined carbon, 0.34 Si, 0.19 Mn, 0.11 S, 0.043 P (analysis of core) was 488 Brinell (90–100 Scleroscope), according to Lippert.[31] Before being hardened the surface was hypoeutectoid. After being hardened, the surface was martensitic changing to troosto-martensitic at junction with the pearlitic core. Nodules of temper carbon were present in the hard case.

Physical Properties

The physical properties of welds in malleable cast iron have been determined by Tilemann,[11] Horstkotte and Zimmermann.[28] Tilemann determined the tensile strength, elongation in tension, maximum load and angular deflection in the transverse beam test, and Rockwell and Brinell hardness of weld and junction zones in metal arc and resistance butt welds prepared in the manner described in the section on Methods of Welding. Tilemann, pressed for space, omitted many details of testing. In Table 3 his results are averaged, using as good judgment as possible. His own results are averages of 5 to 10 specimens for each property, a total of 1860 specimens being tested. Specimens were packed in silica for annealing, and were probably not machined before testing. The analyses of the cast irons are given in Table 4.

Table 4—Analyses of Cast Irons Tested by Tilemann

	Carbon	Combined C	Si	Mn	P	S
Black-Heart Cast Iron	2.38	—	0.78	0.43	0.066	0.050
White-Heart Cast Iron	2.22	0.83	0.75	0.19	0.09	0.20

The conclusions that Tilemann drew from his results are summarized in the section of Method of Welding.

It must be admitted that Tilemann's results with black-heart are not convincing. The strength and ductility of the unwelded black-heart is below par, and the carbon and manganese are slightly above good practice. Furthermore, his choice of annealing treatments (1/2 to 3 hours at 850 to 950° C.) was not fortunate. The high hardness of the weld metal, as-welded, should be noted. Nevertheless, as Tilemann concludes, the results are

Table 3—Mechanical Properties of Metal-Arc and Resistance-Butt Welds in Black-Heart and White-Heart Malleable Cast Iron. Tilemann

Treatment	Tensile Strength, psi	% Elongation L = 10d	Transverse Beam Test Max Load, Lb.	Angle Degrees	Maximum Hardness Base Metal	(Nos. in parentheses are Brinell) Junction Zone	Weld
Black-Heart Malleable Cast Iron				Metal-Arc Welds			
(All specimens 1.18 inches wide × 0.31 inch thick)							
Unwelded	49,000	5	1000	35	Not given		
As-welded	43,000	4	1100	30	B80(146)	C30(285)	C50(495)
Annealed 2 Hr. 900° C.	52,000	1.5	1260	30	B83(156)	B80(146)	B88(174)
(All specimens 0.47 inch diameter)				Resistance-Butt Welds			
Unwelded	61,000			Not given	Not given		
Air-cooled, Re-annealed	43,000	0 to 1	660	" "	" "		
Annealed 2 Hr. 900° C.	67,000	2.5	1280	" "	B85(163)	B90(183)	B92(192)
White-Heart Malleable Cast Iron				Metal-Arc Welds			
(All specimens 1.18 inches wide × 0.31 inch thick)							
Unwelded	44,000	3.2	880	52	Not given		
As-welded	40,000	2	1140	28	B90(183)	C20(223)	C50(495)
(All specimens 0.47 inch diameter)				Resistance-Butt Welds			
Unwelded	53,000	4.2	..	50	Not given		
Annealed 30 min. 900° C.	60,000	2.5	1360	20	B92(192)	B92(192)	B98(2)29

distinctly favorable to welds by either process in either cast iron.

The tensile properties of arc welds in black-heart malleable cast iron containing 2.50 C, 1.00 Si, 0.25 Mn, 0.07 S, 0.160 P are given by E. H. Horstkotte (private communication, March 1937) in Table 5.

Table 5—Tensile Properties of Arc Welds in Malleable Cast Iron. E. H. Horstkotte

Electrode	Tensile Strength, psi	Elongation, % in 2 Inches
Monel metal	33,800	1.00
"	31,650	2.00
"	35,200	3.00
Carbon steel	25,200	1.00
"	23,100	1.00
"	26,700	1.00

The tensile strength of oxyacetylene welds (unmachined) in black-heart malleable cast iron (0.31 inch thick), according to Tilemann,[10] is 72 to 96% of unwelded. Oxyacetylene welds in white-heart malleable developed the following tensile strengths.

Thickness, Inch	Tensile Strength (per cent unwelded)
0.16	120
0.31	90
0.47	80
0.63	66.5

In transverse beam tests (no details), only the specimens 0.16 inch thick had a greater deflection at fracture than unwelded.

The notch impact value of black-heart malleable cast iron (2.6 C, 0.4 Mn, 0.8 Si, 0.1 P, 0.06 S) has been determined by Zimmermann.[28] whose results are shown in Fig. 1. The impact specimen was 0.39 × 0.39 × 2.17 inch, 0.12 inch notch, 0.08 inch diameter, the notch being in weld metal. The results are averages of three specimens at each temperature. Plates of the black-heart iron

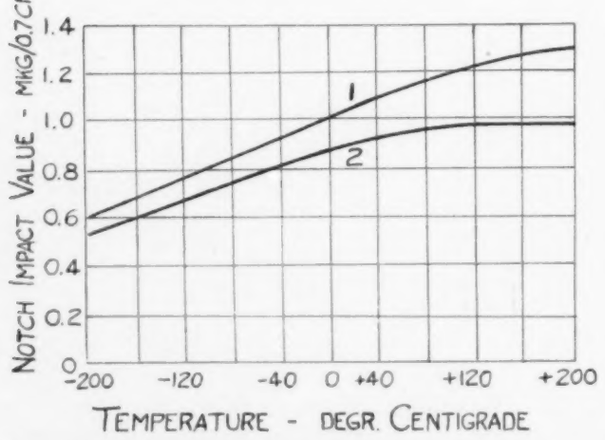

Fig. 1—Notch Impact Value (German Standard Specimen, DVM Round Notch) of Hot Metal-Arc Welds in Black-Heart Malleable Cast Iron. 1—Unwelded, 2—As-Welded (Zimmermann, 1934)

31 inches long, 3.1 inches total width perpendicular to weld, and 0.59 and 0.79 inch thick were 60° X welded (700° C. preheat) with electrodes (0.24–0.31 inch diameter) containing 2.8 C, 2.65 Si, 0.6 Mn, 0.07 P, 0.05 S, 0.04 Cu and cooled in sand. The weld was, of course, gray cast iron and its impact-temperature curve was exactly the same as for welds in gray cast iron. The impact value of the welded specimens was 18 to 30% below unwelded depending on temperature.

Schwartz[22] found (1921) that the strongest bronze welds in black-heart malleable had a tensile strength of 45,000 psi, no elongation, using Parson's bronze (55–60 Cu, 38–42 Zn, 3.5 Mn max., 1.5 Sn max., 1.5 Al max., 0.4–2.0 Fe, 0.20 Pb max.). Failure occurred by tearing at the junction between bronze and malleable.

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Welding High-Tensile Boiler Steels

Summary of an Article by H. Aysslinger, Appearing in *Wärme*, Vol. 60, pp. 220–225, No. 14, April 3, 1937; abstract by G. E. Claussen.†

Tensile, roller-bend and notch impact tests of arc welds in high-tensile boiler steels showed that subsequent heat-treatment was not necessary to develop properties in the weld equal to those of base metal. Welds were made in the steels shown in Table 1. In 0.47-inch plate the welds were 70° V, 0.04 inch root spacing; in 0.71-inch plate the welds were either 80° X or 60° V, without root spacing. Four welders were employed for the joints. The electrode was the covered Gutehoffnungshütte "Pan." Deposited metal contained 0.08 C, 0.09 Si, 0.51 Mn, 0.038 P, 0.027 S. The properties of all-weld-metal deposited in an angle iron were:

Yield Point: 56,700 psi (average of 5 specimens)
Tensile Strength: 72,200 psi (average of 5 specimens)
Elongation: 24.8% in 5 inches (average of 5 specimens)
Bend Elongation: 40% in 0.59 inch (180° bend)
Notch impact value: 10.8 mkg./cm.² (average of 8 specimens).

The tensile specimens were 0.47 to 0.57 inch diam. The bend specimens were 0.39 inch thick; roller diameter and span were 2 inches, and the plunger width was 0.79 inch.

Table 1—Chemical Composition and Tensile Strength of Unwelded Base Metal

Boiler Plate	C	Si	Mn	P	S	Tensile Strength, psi
II	0.19	..	0.44	0.020	0.021	63,000
III	0.20	0.23	0.65	0.018	0.018	68,500
IV	0.21	0.34	0.77	0.015	0.026	74,000

Tensile Tests

Tensile tests were made at room temperature on parallel-section and reduced-section specimens with welds machined flush. The parallel-section specimens fractured well beyond the joint, average results being shown in Table 2. The results of the reduced-section specimens are shown in Table 3. The results are the lowest and highest values of three specimens, each 1.42 inches wide, reduced at the weld to 0.95 inch, the radius being 2 inches. All specimens fractured through the weld except the weakest specimen of plate II, which broke through the fusion zone.

Table 2—Tensile Tests on V and X Welded Rectangular Bars (0.71 × 2.13 Inches Cross Section, 12 Inches Long)

Material	Type of Weld	Yield Point, psi	Tensile Strength, psi	Elongation Over the Weld; % in 0.59 Inch	Reduction of Area, %
Boiler Plate II	V	35,000	61,000	7.3	5.7
" " "	X	36,200	60,000	7.7	6.6
" " III	V	43,000	70,500	8.5	11.5
" " "	X	43,700	71,500	11.0	17.0
" " IV	V	47,500	75,000	13.0	10.0
" " "	X	47,000	74,000	17.0	13.0

† Research Assistant, Welding Research Committee.

Table 3—Tensile Tests on V-Welded Reduced Section Specimens in 0.47-Inch Boiler Plate

Material	Tensile Strength, psi
Boiler Plate II	71,000–73,500
Boiler Plate III	80,500–83,000
Boiler Plate IV	80,500–82,500

The results of short-time tensile tests at 250° C. are shown in Table 4. The 0.47-inch specimens (machined flush) were square in cross section; the 0.71-inch joints were machined to round specimens, 0.63 inch diam. Fracture occurred outside the welded zone in all specimens.

Roller-Bend Tests

The standard German roller-bend test was used: roller diameter 2 inches for 0.47-inch welds, 4 inches for 0.71-inch welds; roller distance = 5 t, plunger thickness = 2 t, where t = thickness of weld.

Of 45 root bend V welded specimens in the three steels, 43 gave 180° bend angle. A bend angle of 180° in face bend specimens was obtained only if porosity was absolutely absent from the root.

Table 4—Short Time Tensile Tests at 250° C.

Material	Type of Weld	Yield Point, psi	Tensile Strength, psi	Elongation of Weld, in 0.59 In.	Reduction of Area, % Plate	Reduction of Area, % Weld
Boiler	V	31,400	76,000	4.0	39.0	3.0
Plate II	V	33,000	76,500	5.0	34.0	2.0
0.47 inch thick	V	34,200	78,300	5.0	32.0	4.0
Boiler	X	34,000	82,000	9.0	..	9.0
Plate III	X	35,000	80,500	7.0	..	7.0
0.71 inch thick	X	34,700	79,500	7.0	..	6.0
Boiler	X	39,500	63,000	10.0	...	...
Plate IV	X	39,200	84,500	7.0	47.0	8.0
0.71 inch thick	X	39,000	82,000	7.0	46.0	7.0

Face Bend.—Bend elongations for welds in the three steels varied from 36 to 41 % in 0.59 inch for 0.47-inch welds, and from 27 to 40% for 0.71-inch welds.

Bend tests at room temperature on V and X welded specimens (0.71 inch plate) quenched in water (28° C.) from 650° C. and then tested at once or after being immersed for 2 hr. in boiling water resulted in a bend angle of 180° in all cases. The bend elongation varied from 25 to 45% in 0.59 inch. The bend ductility of unwelded specimens was zero to 50% greater than welded in steels

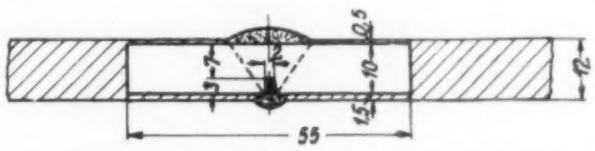

Fig. 1—Position of the Notch of the Notch-Impact Specimen for Welded Plate 0.47 Inch Thick. Dimensions in Millimeters

II and III but was zero to 50% less than welded in steel IV. The treatment in boiling water had no effect.

Notch Impact Tests

Standard German notch impact specimens (10 x 10 x 55 mm., 0.39 x 0.39 x 2.17 inch) were machined from unwelded plate (notch parallel as well as perpendicular to direction of rolling) and from as-welded specimens in 0.47-inch plate. The location of the notch in the welded specimen is shown in Fig. 1. Tests were made at −60 to +600° C., the blow being struck 2 to 3 seconds after

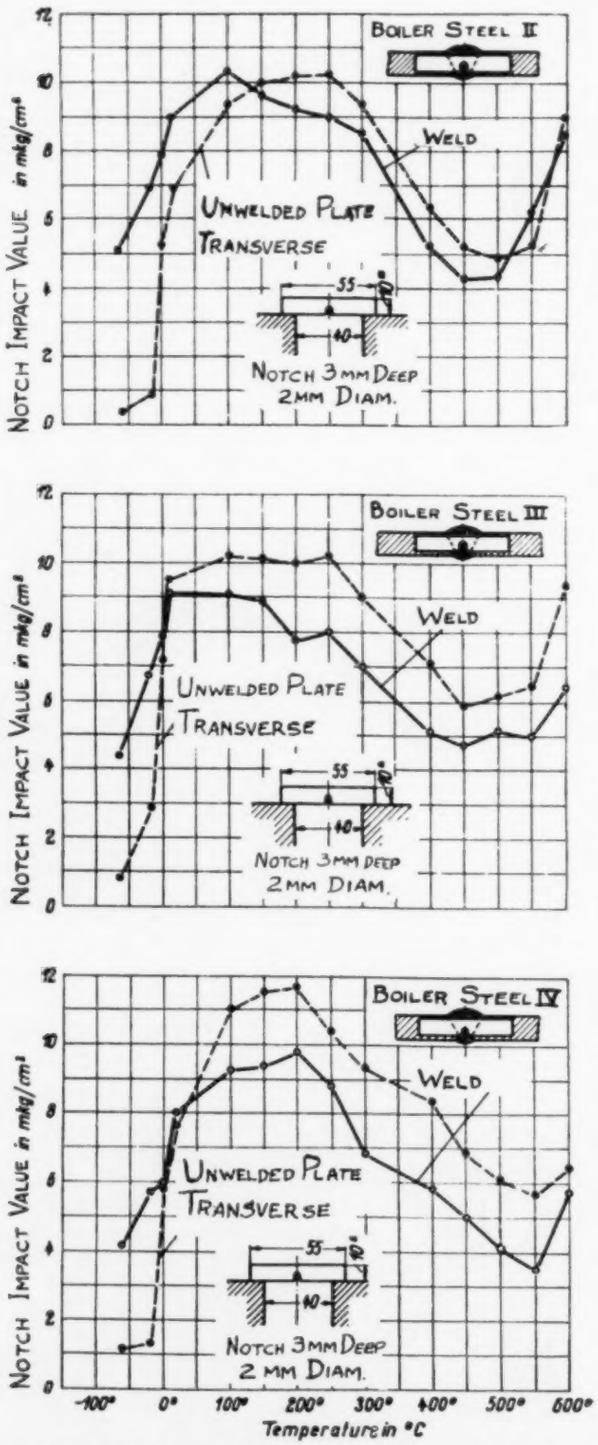

Fig. 2 (Upper), 3 (Middle) and 4 (Lower)—Effect of Temperature on Notch Impact Value of Welded and Unwelded Boiler Steels II, III and IV, 0.47 Inch Thick. Dimensions in Millimeters

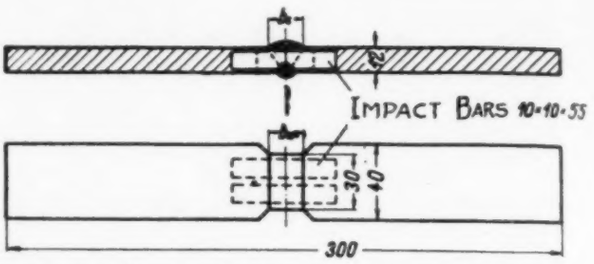

Fig. 5—Position of Notch-Impact Specimens in Strained Bar, 0.47 Inch Thick, 11.5 Inch Long (B = Breadth of Weld at Face)

the specimen was removed from cooling bath or furnace. The results are shown in Figs. 2 to 4. The results are averages of three specimens at each temperature. In general, the results for the transverse specimens were up to 50% lower than the longitudinal. The results show that the welds have lower notch impact value than unwelded in the range 20 to 600° C. In the temperature range +20 to −60° C., however, the welds are definitely superior to unwelded specimens. At −60° C. the unwelded specimens had a completely brittle fracture, whereas the welds showed considerable ductility.

Quench-Impact Tests

Table 5 contains the results of Charpy tests on quenched specimens. The specimens were 15 x 30 x 160 mm. (0.59 x 1.18 x 6.3 inch) with a cross section of 0.39 sq. in. back of notch. The span was 4.7 inches. The quenching did not have a serious effect on any of the specimens.

Table 5—Charpy Value of V-Welded Specimens of Three Boiler Steels 0.71 in. Thick After Different Heat-Treatments

Heat-Treatment	Notch Impact Value Mkg./Cm.² (Average of 4 Specimens) Boiler Steel II	Boiler Steel III	Boiler Steel IV
None	13.8	11.4	19.3
Quenched from 650° C. in cold water; tested at once	17.2	14.5	9.9
Ditto, tested after annealing 2 hrs. in boiling water	15.6	13.1	9.2
Quenched from 750° C. in cold water, tested at once	16.4	17.5	13.3
Ditto, te ted after annealing 2 hrs. in boiling water	11.4	13.0	13.5

Strain Impact Tests

Samples of V welds in 0.47-inch plate, shown in Fig. 5, were slowly elongated or compressed about 10% in a tensile machine. The specimens were then annealed one hour at 250° C., after which notch impact specimens (10 x 10 x 55 mm.) were machined from the specimens. The results are shown in Table 6. Unwelded specimens of steels III and IV had a notch impact value of 3.0 mkg./cm.² after being compressed 10% and aged 1 hr. at 250° C. The strain-impact properties of welds, all of which fractured with evidence of ductility (not brittle), are thus superior to those of unwelded specimens.

Table 6—Notch Impact Tests on Strained V-Welds in 0.47-Inch Steel

Strain	Heat Treatment	Notch Impact Value Mkg./Cm.² Boiler Steel II	Boiler Steel III	Boiler Steel IV
10% elongated	½ hr. at 250° C.	4.3 to 4.4	not det'd	not det'd
10% elongated	1 hr. at 250° C.	4.7 to 5.0	3.0 to 3.4	4.3 to 5.4
10% compressed	1 hr. at 250° C.	not det'd	3.1 to 4.0	3.2 to 3.8

THE ENGINEERING FOUNDATION
WELDING RESEARCH COMMITTEE

Sponsored by American Welding Society and American Institute of Electrical Engineers

Supplement to the Journal of the American Welding Society, July 1937

Published Reports

The following reports of the Welding Research Committee have been published thus far in the AMERICAN WELDING SOCIETY JOURNAL:

Fundamental Research Division

1935

Arcs in Inert Gases—G. E. Doan and A. M. Thorne, April.

Effect of Peening on Physical Properties of Welds and Stress Relief—O. M. Harrelson, May.

Magnetic Characteristics of Metal Deposited by the Oxyacetylene Welding Process—Wendell F. Hess and Robert Lee Ringer, Jr., June.

The Design of Welded Seat Angle Connections—Flexible Type—Heath Lawson, June.

The Development of Weld Testing as Indicated by Published Literature—M. F. Sayre, Aug.

Direct Measurement of Contraction of Butt Welds—O. M. Harrelson and Searcy B. Slack, Aug.

A List of Fundamental Research Problems in Welding, Aug. (1935).

Some Residual Stress Effects in Static Tension Specimens—R. E. Jamieson, Sept.

The Behavior of Fillet Welds When Subjected to Bending Stresses—Norman G. Schreiner, Sept.

Advantages of Welding in Continuous Structures—Inge Lyse, Oct.

Tests to Determine the Feasibility of Welding the Steel Frames of Buildings for Complete Continuity—W. M. Wilson, Oct.

Report of Creep Tests for Gas Welded Steel—N. F. Ward, Oct.

Spot Welding Problems—J. H. Zimmerman, Dec.

1936

Arc Welding in Argon Gas—Gilbert E. Doan and W. C. Schulte, Jan.

Tests to Determine the Feasibility of Welding the Steel Frames of Buildings for Complete Continuity—W. M. Wilson, Jan.

Advantages of Welding in Continuous Structures—Inge Lyse, Feb.

Progress in Welding During the First Six Months of 1935—W. Lohmann and W. Schneider, March.

Progress in Welding During the Second Half of 1935—W. Lohmann and W. Schneider, June.

The Metallurgy of "Pure" Iron Welds, Gilbert E. Doan and W. C. Schulte, Sept.

Effect of Weld Penetration on Stresses in Fillet-Weld Joints, Sept.

Proposed Impact Test Specimen—A. Vogel, Sept.

Welded Beam-Column Connections, Oct.

The Effect of Low Temperature on the Tensile Impact Resistance of Iron, Steel and Welded Joints—Otto H. Henry, Oct.

Welded Structural Brackets—Cyril D. Jensen, Oct.

Electric Heating by the Proximity Effect—Edward Bennett, Oct.

Characteristics of a Universal Welding Generator—N. F. Ward, Nov.

A List of Fundamental Research Problems—Nov.

Micro Testing Machine, Chevenard, tr. by M. Scioletich, March.

1937

Progress in Welding During Last Six Months of 1936, April.

Strain Measurements in Welded Joints, by J. L. Banville and H. A. Storrs, June.

An Investigation of Plug and Slot Welds, Glenn James Gibson, July.

Peening and Its Effects on Arc Welds, E. M. MacCutcheon, Jr. and D. M. Kingsley, Jr., July.

Literature Division

1935

Bend Testing of Welds—A Summary—M. F. Sayre, Dec.

1936

Impact Tests of Welded Joints, by W. Spraragen and G. E. Claussen, April.

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Fatigue Strength of Welded Joints—A Review of the Literature to Oct. 1, 1936, by W. Spraragen and G. E. Claussen, Jan.

Welding of Copper and Its Alloys—A Review of the Literature to Jan. 1, 1936, by Ira T. Hook, Feb. and March.

Welding of Cast Iron—Review of Literature to Nov. 1, 1936, by W. Spraragen and G. E. Claussen, March.

Welding of Cast Steel—A Review of the Literature to November 1, 1936, by W. Spraragen and G. E. Claussen, April.

Stress Distribution in Fillet Welds—Review of the Literature to January 1, 1937, by A. G. Solakian and G. E. Claussen, May.

Welding Special Ferrous Alloy Castings—A Review of the Literature to November, 1936, by W. Spraragen and G. E. Claussen, June.

Welding Malleable Cast Iron—A Review of the Literature to October 1, 1936, by W. Spraragen and G. E. Claussen, June.

Shrinkage Distortion in Welding, July.

Welding Aluminum and Its Alloys (German abstract), July.

Industrial Research Division

Arc Welding of Structural Alloy Steels—W. L. Warner, Oct. 1936.

The Nickel-Copper High-Strength Steels for Welded Construction, by A. E. Gibson, Feb. 1937.

Welding High-Tensile Boiler Steels, by H. Aysslinger, June 1937.

Electric Arc Welding of Monel Metal Structures, by J. F. Maguire, July.

Loss of Material in Flash Welding and Automatic Flash Welders, by E. Rietsch.

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Literature Reviews Favorably Received

In connection with many of its activities, notably Literature, the Welding Research Committee has received many favorable comments. A few of these selected at random are reproduced below.

The Welding Research Committee would welcome the comments of all members of the AMERICAN WELDING SOCIETY and others interested, as well as any constructive suggestions for work to be done. The aim of the Welding Research Committee is to serve the welding industry through cooperative effort in welding research matters.

Welding Special Ferrous Alloy Castings

"I have read with great interest your paper and I congratulate you on your work, which I think is a most useful review of the present stage of knowledge now achieved on the subject.

"I take advantage of this occasion to tell you that I have read with most interest the different papers published recently in the JOURNAL. In my opinion these papers represent a most useful review of the present state of testing welded materials."

W. BONHOMME, Institut de Metallurgie, University of Liege, Belgium

"I would like to take this opportunity of expressing appreciation of the Supplement on Welding Cast Iron issued March 1937. This is very comprehensive and useful. With respect to the Supplement on Welding Special Ferrous Alloy Castings, you appear to have covered the directions in which we have worked, particularly those on austenitic cast irons, and I do not think we have any further information to add."

J. G. PEARCE, Director, The British Cast Iron Research Association

Impact Tests of Welded Joints

"In my opinion this monograph covers completely its field: and—according to my knowledge—all the statements it contains are perfectly correct.

"I will be obliged to you if you send me other reviews, as I am very much interested."

FEDERICO GIOLITTI, Via Ottavio Revel 20, Turin, Italy

"I have read with very great interest the Review on Impact Tests of Welded Joints.

"The Review, and the others of the series which you propose, should be of the very greatest value both to research workers and to everybody who is trying to obtain a comprehensive view of the present position of knowledge of welding subjects.

"We would like very much to have copies of the further reviews as they are issued."

A. RAMSAY MOON, Murex Welding Processes, London

"I want to thank you for your kind letter of June 19th as well as for the paper which accompanied it. All similar communications will be welcome.

"I was deputed at the last Welding Congress in London to prepare a report on weld tests. If this report is printed, as I think it will be, I will send you a copy; unfortunately this report is not so complete as yours."

A. PORTEVIN, 32 Boulevard de la Chapelle, Paris, France

Fatigue Strength of Welded Joints

"I commend you and Dr. Claussen on the fine Review which will be of assistance to all, whether or not conversant with Fatigue."

OTTO GRAF, Professor für Baustoffkunde und Baustoffprüfung an der Technischen Hochschule, Stuttgart, Germany

"It need scarcely be added that the authors of this report are to be heartily congratulated for their exhaustive and important study. No doubt, their work will be of help to anybody interested in problems of welded constructions."

Dr. D. ROSENTHAL, Laboratoire

(*Continued on page 28*)

Electric Arc Welding of Monel Metal Structures*

By JAMES F. MAGUIRE†

THIS paper is intended to give a brief description of the methods and experiences encountered in the manufacture of monel metal structures by arc welding at Watertown Arsenal. Monel metal referred to in this paper is a high strength, copper-nickel alloy of the following composition and properties:

Analysis

C	Mn	Si	Fe	Al	Cu	Ni
0.30 max.	3.5 max.	0.5 max.	3.5 max.	0.5 max.	23 min.	60 min.

Physical Properties

Tensile Strength	90,000 lb. per sq. in.
Yield Strength 0.05% Set	55,000 " " " "
Elongation in 2 Inches	18%
Reduction of Area	30%

The monel metal electrode used in welding these structures is of the covered type, purchased under specifications which require the electrodes to be of the same composition as the plate except that the aluminum content is to be 2 to 2.75 % unless otherwise specified.

A check analysis made from a sample of the monel metal plate gave the following results:

C	Mn	Si	Fe	Al	Mg	Cu	Ni
0.10	0.99	0.045	1.28	1.85	0.012	31.25	66.14

Physical Properties

Tensile Strength	95,500 lb. per sq. in.
Proportional Limit	54,500 " " " "
Elongation in 2 Inches	25%
Reduction of Area	64.2%

Analysis of the monel metal electrodes gave the following results:

C	Mn	Si	Fe	Al	Mg	Ti	Cu	Ni
0.175	0.51	0.32	1.21	2.65	0.008	0.42	29.15	65.56

The monel metal used in the fabrication of these structures is purchased either cold drawn, hot rolled, low temperature finished or forged. Cold drawn monel is received stress relief annealed from the manufacturer and is formed to shape by machining only. When processing such as bending, forging or forming, the metal must be identified as to the original method of fabrication, i.e., forged, hot rolled or low temperature finished.

Monel metal is "red short;" that is, it lacks ability to flow within certain temperature limits, and work done on the metal within this range is apt to cause rupture. The use of pyrometric control to check temperatures during forging is very essential. Safe forging temperature is between 1010° C. to 1177° C. Low temperature finished monel metal plate will have its physical properties lowered beyond acceptance values if heated above 600° C. When monel metal is heated, care must be exercised to prevent the pick-up of sulphur, which causes brittleness. Electric or gas fired furnaces are considered the best for heating.

When low temperature finished monel metal plate was subjected to severe cold working, such as occurs when metal is rolled into a cone, or formed to shape cold, it was found difficult to prevent cracking of the weld, unless the plate was given a stress-relieving treatment of 575° C. for five hours before welding. An example of our experience with this trouble was found while welding a "U" shape plate made from $^1/_2$-inch thick low temperature finished monel metal plate.

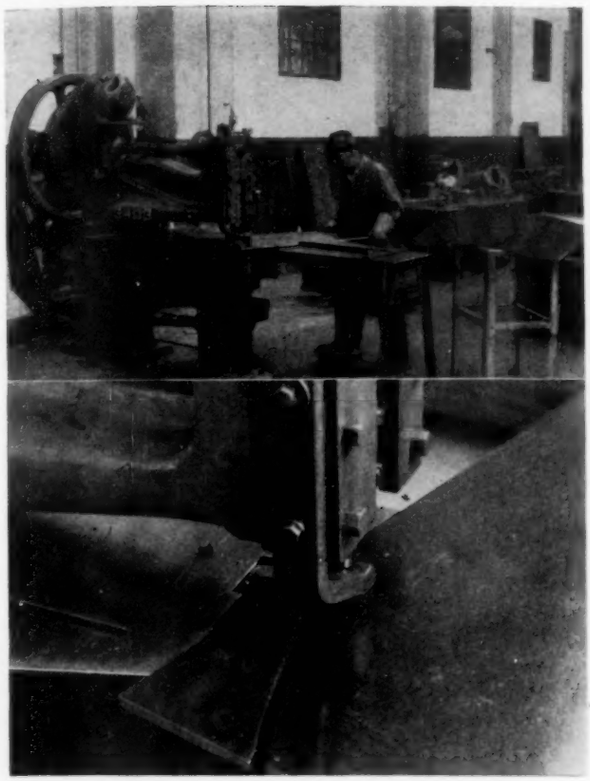
This plate was formed cold into a "U" shaped piece under a press. The corners were set cold on an anvil to form a right angle. The ribs, plates and stiffeners were then tack-welded to this plate without evidence of welds cracking, until welding was attempted around the right angle corners and up the sides of the plate in the region where severe cold working took place. It was found impossible to make a weld in this region without the weld cracking through the center of the weld metal and for the entire length of the weld. It was first thought that the sequence of welding the various parts to the plate was causing the trouble. One by one these pieces were cut loose until finally a single bead was made on the side of the plate in the direction of the bend. The weld cracked in several places at right angles to the bead, indicating that high stresses were set-up in the plate. The plate was then stress-relieved at 575° C. and held for three hours. This treatment materially reduced the cracking of the weld metal but did not cure it. The plate was again stress-relieved at 575° C. but was held at this temperature five hours instead of three hours. No further trouble was found.

The general procedure adopted for fabricating welded monel metal structures is divided into the following steps or sequence of operations:

1. Preparation of templets for cutting out irregular shapes
2. Shearing
3. Cutting out irregular shapes on nibbling machine
4. Machine scarfing of faces for welding
5. Forming or rolling to shape of components entering into welded assembly
6. Stress-relieving those parts subjected to cold working while forming to shape
7. Fitting, setting and tacking of components before welding, using jigs
8. Welding
9. Visual Inspection
10. Preliminary layout of structures to determine finish machining dimensions
11. Stress relieve finish structures
12. Sand-blast
13. Radiographic Inspection
14. Final Layout
15. Machining

* Contribution to the Industrial Research Subcommittee. Published with the permission of "The Chief of Ordnance," U. S. Army.

† General Foreman, Structural & Welding Department, Watertown Arsenal.



Figs. 1 and 2—Nibbling Machines Cutting 1/2-Inch Monel Metal Plate

Explanation of Procedure

1. Preparation of Templets for Cutting Out Irregular Shapes.—Templets for the various irregular shapes are made from 1/8-inch thick steel plate. The templets are made light and strong so that they may be handled easily. The templet is identified by stamping the drawing number and piece mark of the component for which templet is to be used.

2. Shearing—Straight line shearing is accomplished on standard commercial plate and angle shears.

3. Cutting Out Irregular Shapes on Nibbling Machine.—Due to the composition of monel metal, gas shape cutting machines could not be used for cutting plates to shape. Excellent results were obtained for this kind of work by using a standard commercial nibbling machine as shown in Fig. 1.

The nibbling machine is of the roller die type equipped with mechanical feed for advancing the sheet to the cutting tool, and so arranged that cut can be started or stopped at any given point by the action of the foot treadle, and so arranged that the sheet being cut is held by rolls under compression through spring action while cutters are in operation.

Figure 1 shows the nibbling machine in operation. (Note the method of cutting radius by welding radius bar to edge of plate and pivoting arc on adjustable pin on machine table.)

Figure 2 shows close-up photo of 1/2-inch low temperature finished monel metal plate being cut to shape on nibbling machine.

4. Machine Scarfing of Faces for Welding.—Figure 3 shows method of scarfing faces for welding butt joints. This operation is performed in a small horizontal boring mill. The half section of a cone is held in a clamping fixture. (Note the double-angle cutter scarfing in one pass the edge of a butt joint for welding.)

5. Forming or Rolling to Shape of Components Entering Into Welded Assembly.—The rolling or forming into cylindrical or cone shapes of low temperature finished monel metal plate, while cold, has not been accomplished in what could be called a satisfactory manner. Due to the extreme toughness of the material, this plate does not respond to forming, using the same methods or machines as are used on steel.

The present method, which is considered unsatisfactory, is to roll half sections of cone to shape on bending rolls.

Study is being made as to the advisability of forming cylindrical or cone shapes using swaging dies.

6. Stress-Relieving Those Parts Subjected to Cold Working While Forming to Shape.—Half cones, cylinders or shapes that are stress-relieved before welding, due to being highly stressed in forming cold, distort badly unless precautions are taken to hold them in shape while being heated. We have found that a simple way to help these components hold their shape is to tack weld tie bars to projecting edges or sections; also, to place the pieces in the furnace so that they are free to expand and contact. A cast-iron table placed on the floor of the furnace so that the components can be easily leveled has been found to help prevent distortion, especially in stress-relieving heavy structures of intricate shapes.

8. Welding.—Due to the tendency of monel metal to distort from the heat in welding, it was found more satisfactory to weld sub-assemblies first before welding them into a complete structure. If distortion takes place in welding these sub-assemblies, it can be corrected more easily than if all the components were welded as a unit.

As very little welding data were available for welding low temperature finished monel metal plate, 1/2 inch or over in thickness, considerable research work was necessary to develop a welding electrode which would deposit sound, ductile weld metal without cracking. The welding electrode finally adopted was approximately the same composition as the monel metal plate except it contained 2.65% aluminum and 0.42% titanium.

The following example of one test made with the results may prove of interest. An "H" beam, Fig. 4, was prepared from three pieces of 1/2-inch thick low temperature finished monel metal plate, 28 inches long by 6 inches wide. One of the pieces formed the web and the other two pieces the flanges. The triangular flange stiffeners shown are 3 inch x 2 3/4 inch x 1/2 inch spaced six on each side of web plate. The piece was welded with 5/32-inch diameter monel electrode (Inco #130X). The fillet welds were 1/2 inch and made in two layers.

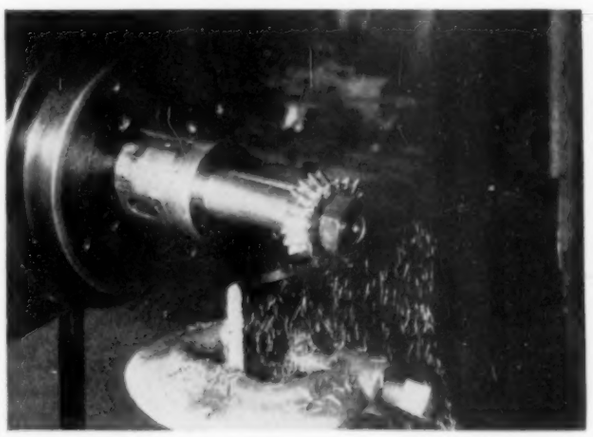
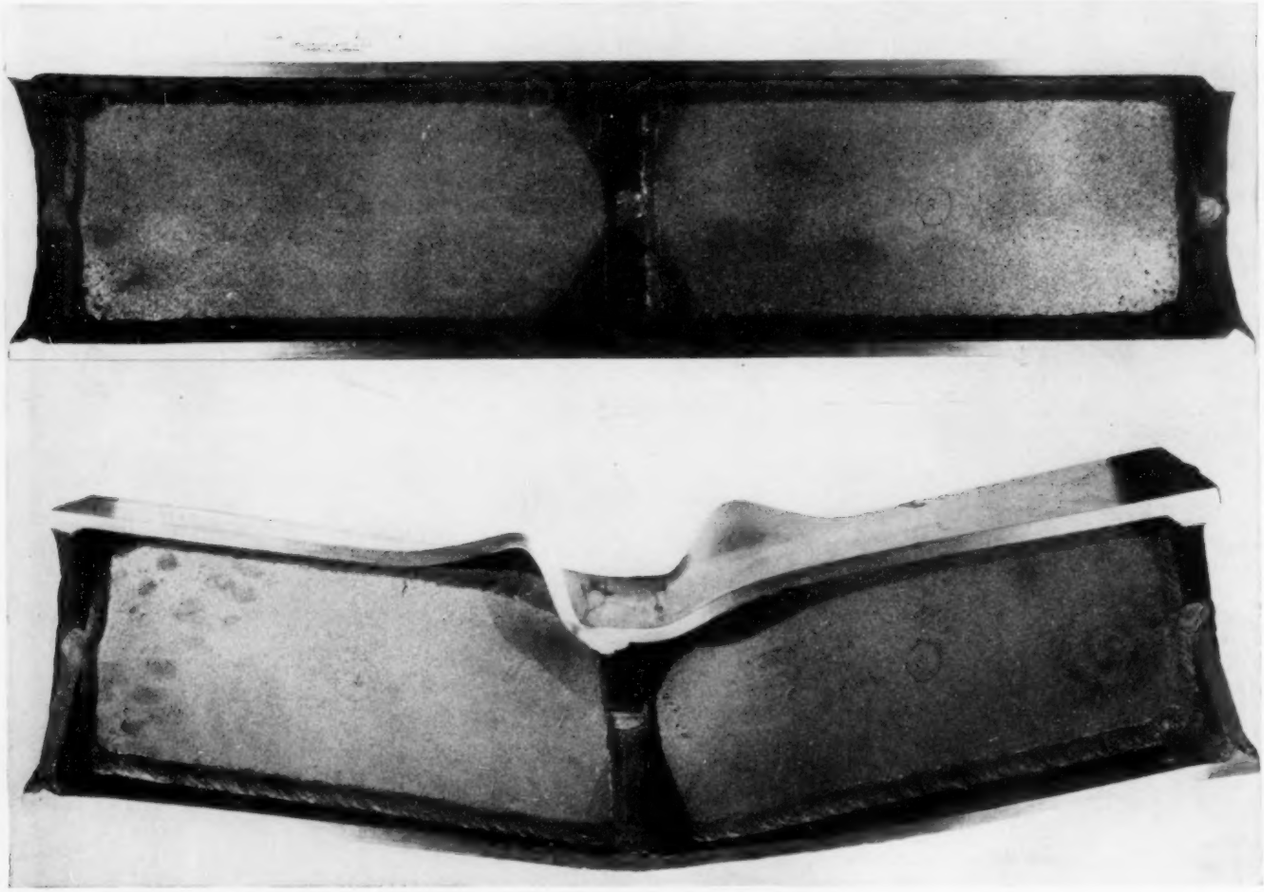


Fig. 3—Scarfing Machinery



Figs. 4 and 5.—Experimental Welded "H" Beam 1/8-Inch Monel Plate

Approximately 150 amperes, 25 volts reversed polarity (electrode positive) across the arc was used. The welding was all done in a flat position. The H-beam was then stress-relieved. The piece was checked before and after stress-relieving and showed no change in shape due to stress-relieving. The piece was then X-rayed completely and showed evidence of lack of penetration in a few places between the flange and web plates. The beam was next machined by removing approximately $^1/_8$ inch of metal from the top and bottom flanges and along the edges. The piece was then measured accurately, using a micrometer, and allowed to stand for four days when it was again measured. There was no change of dimension found. The beam was then tested to failure in the physical testing laboratory, as shown in Fig. 5. A pronounced yielding started at 150,000 pound load by buckling of the top flange in compression. It is significant that even though the weld fillets had been bent and stretched longitudinally and transversely there was no evidence of weld failure.

The fluidity of monel metal electrodes makes it necessary to weld in a flat position. In order to insure ease of operation and to invite good work from the operator, motor driven tilting tables, Fig. 6, have been provided. With the aid of push button controls the operator may revolve his work 360° or tilt it forward or backward so that no manual labor is involved to bring the work into a flat position.

It was found that the best results were obtained by making the weld in two or more layers. The first layer should not be less than $^1/_4$ inch high. For butt welds, it was found that the best results were obtained by using

Fig. 6

a bevel $37\frac{1}{2}°$ on each side of joint or double angle of 75°, spacing the joints with $\frac{1}{16}$-inch opening between them.

When making butt joints by welding from both sides, it was very essential to weld about halfway through the joint and then to remove the slag at bottom of vee before completing the weld from the opposite side of the joint. It was also found that the best results were obtained by skip welding around the structure, not welding stiffeners

Fig. 7—Close-Up of Weld

or ribs until last, and then to prevent locking up stresses, welding from the corners to the outer edge of the stiffeners or ribs. In welding monel, it was found that the metal was very susceptible to crater cracks if arc was broken quickly at the end of the run. By choking the arc, allowing the crater to fill up, this trouble was greatly reduced.

The penetrating quality of the present monel metal electrode is not comparable with the coated steel electrodes. It does not penetrate like steel. The weld metal is also very fluid. Due to this behavior, it is necessary to scarf all fillet and butt joints to get penetration, and also to make weld in a flat position.

9. Visual Inspection.—All welded structures are given a visual inspection immediately after welding to bring out any external defects which should be corrected before heat treatment. This practice has been found very satisfactory as it has enabled repairs to be made which, in some cases, would be cause for reheat treatment if made after heat treatment.

11. Stress Relieve Finished Structures.—All welded monel metal structures are given a stress-relieving treatment. This treatment consists of heating the structure slowly to 575° C. and holding for five hours, and then allowing structure to cool in the furnace.

12. Sand-blast.—Sand- or shot-blasting is considered the most practical means for cleaning scale and slag from weld metal preparatory to inspection.

13. Radiographic Inspection.—X-rays are used very extensively for developing welding procedure and also for inspection of completed welded structures. As a means to assist the supervisors in developing welding technique and to control the quality of work turned out by the welding operators, X-ray has proved a very valuable shop tool.

Figure 7 shows a typical weld on a heavy monel metal structure. This figure shows the method of joining a $2\frac{1}{2}$ x 5-inch rail to a tube $10\frac{1}{2}$-inch O. D. by $8\frac{5}{8}$-inch I. D. The weld is a 1-inch fillet made in five passes. It was found difficult to weld a rectangular shape to the cylinder without serious undercutting. This trouble was overcome on the final layer by running a single bead parallel with the tube at the edge of the weld joining the tube to the rail, and completing the welded joint on the last pass by bridging the weld at right angles to the tube, allowing the weld metal to fuse into the single bead instead of into the tube.

Where a number of structures of the same design are to be made, it has been found good practice to build a pilot structure first, to insure that drawings are correct, that regions to be welded are accessible and that proper welding technique is established.

In conclusion, it has been the experience at Watertown Arsenal that the fabrication of heavy monel metal structures by arc welding can be readily accomplished if all operations are carefully planned, and welding operators are trained in the welding technique for monel metal.

Loss of Material in Flash Welding and Automatic Flash Welders

By E. RIETSCH

Extended abstract of two papers by E. Rietsch, Berlin, appearing in *Elektroschweissung*, 8, 41-43, March 1937, and *Maschinenbau*, 16, 25-28, Jan. 1937. Translated by Dr. G. E. Claussen, Research Assistant, Welding Research Committee.

THE decrease in length as a consequence of flash welding two bars is due to actual loss of material and to upsetting. The decrease is usually determined experimentally for every set-up, there being no general theory concerning the loss. Two factors are at work: Heating and "Purification."

Heating

Heating may be purely by the flash effect or may be supplied by preheating. Even if the bars are preheated to welding heat the flashing operation must provide some heat in distributing the welding heat uniformly over the surfaces to be welded. With highly preheated specimens it has been observed that the heated zone becomes smaller instead of larger during flashing. The welder calls this "burning out of the hot metal," and associates it with "cold upsetting." This phenomenon is observed particularly with compact cross sections near the maximum capacity of the machine. Flashing thus has an effect on the depth of heat which has not yet been investigated.

To understand the situation more clearly, imagine a very long time of flashing with correspondingly slow push-up. Under these conditions a stable state is reached with respect to both flashing velocity and depth of heated zone. When the stable state is attained the flashing velocity no longer increases but becomes constant. Experiments on hand welders have proved this statement.[1]

[1] See Kilger, H., *Fertigungstechnik und Güte abbrenngeschweisster Verbindungen*, F. Vieweg & Sohn, Braunschweig, Germany, 1936, Pg. 24, Fig. 10.

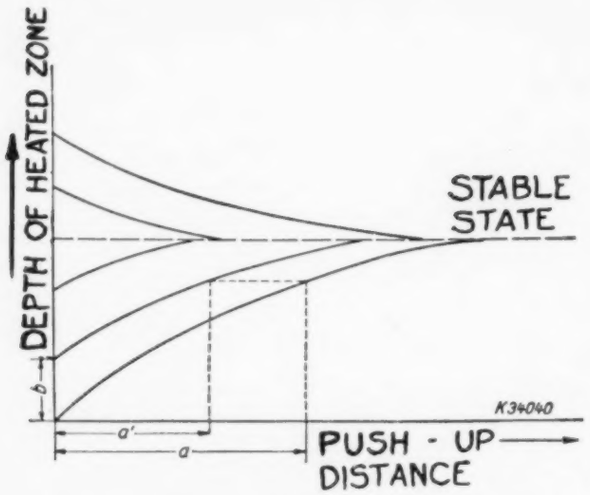


Fig. 1—Relation Between Depth of Heated Zone and Flashed Length

The depth of heated zone also becomes constant. It is immaterial whether preheating was or was not used once the stable state has been reached.

In other words, the stable state may be approached from either side, as shown in Fig. 1. The depth of heated zone may be regarded as the distance from the end to be welded to the end of the red hot zone. Depending upon the side from which the stable state is approached the heated zone may grow longer or shorter during flashing. However, the same depth of heated zone is attained in the stable state, no matter how it has been approached. In Fig. 1 the final effect is the same whether the material is cold to begin with and a distance A inches is flashed, or the metal is preheated to B and flashed only for a distance A′ inches. It should be clear from this example that the necessary flashing distance is greatly influenced by the procedure of welding.

The depth of heated zone determines the upset pressures, and vice versa. It is thus evident that attempts to define the minimum upset pressure for a given strength of weld are absurd unless all welding conditions are specified. Even if the absolute optimum pressure were to be determined from a large number of tests involving many variables, the results would be of questionable utility.

Figure 1 shows what steps should be taken to adapt a low-power machine to a given weld of rather large cross section. In this case high preheating is essential, but, as shown in Fig. 1, the upset distance A must not be too large, otherwise cold upsetting occurs. Preheating thus reduces the push up and saves material (e.g. high-speed steel). The loss of material due to scaling during preheating is negligible. It might be thought that if preheating were carried far enough flashing would be superfluous. This is absolutely not the case, because "purification" in flash welding is the second essential factor.

Purification

The reason for the high strength of flash welds compared with any other type of fusion weld is their freedom from oxygen and nitrogen despite the fact that the flash weld is made with free access to air. It used to be thought that the purity of flash welds was explained by the squeezing out of all slag from the welded section. Actually, the explanation lies in the fact that flash welding automatically excludes air from the surfaces to be welded. The arcing that occurs during flashing creates gases that sweep away all air from the flashing surfaces.

The purifying action of the flashing process consists first of the scaling away of a definite amount of metal during preheating. The oxide thus formed is then expelled by the vapors produced by arcing. The action is best illustrated by the flash welding of the open ends of two otherwise closed pipes. The weld is never successful because the air in the pipe expands during welding and escapes through holes in the weld.

The validity of the vapor theory of flash welding is shown also by the fact that the current must not be switched off even a fraction of a second before upsetting. The ill effects of switching off the current too soon are not to be explained by the accompanying insignificant drop in temperature. Rather, the cessation of arcing permits sudden entrance of air.

Summarizing, we may say that decrease of length in flash welding is not a constant quantity, but varies with the welding conditions. An approximate diagram of the loss of length to be expected in flash welding is shown in Fig. 2. The diagram applies to average conditions, and is to a great extent independent of the steel to be welded. The values are the total loss of length due to preheating, flashing and push up. For example, a section 1 inch square loses about 0.4 inch in length during flash welding. The same loss would be found if the breadth of the section parallel to the gripping dies was $^1/_2$ inch and the height perpendicular to the dies was $2^3/_4$ inch. It may often be possible to obtain still lower losses than those indicated by Fig. 2.

In an earlier article[2] Rietsch deals with other aspects of flash welding. Oscillograms show that the variation of current with time is very irregular, the duration of each arc being about $^1/_{10}$ of a half-cycle of 50 cycle A.C. (about $^1/_{1000}$ second). A schematic diagram of the flashing process is shown in Fig. 3. The bridges of metal are clearly visible in incompletely flash welded bars.

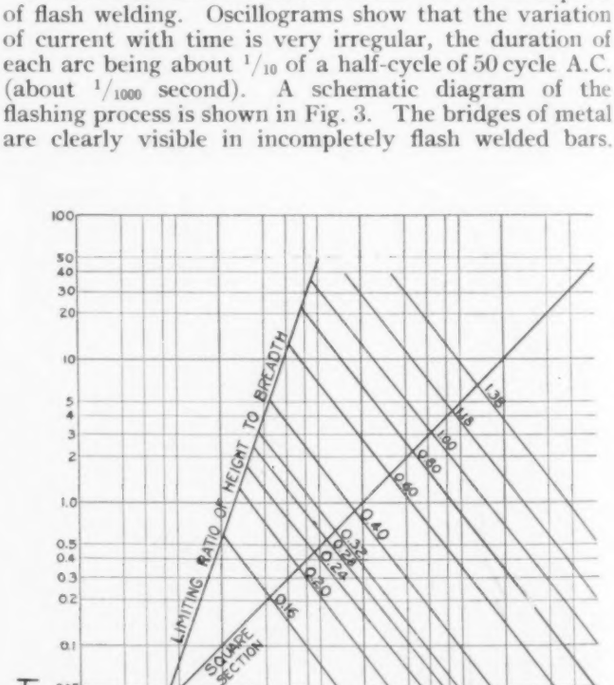


Fig. 2—Total Loss of Length in Inches During Flash Welding as a Function of Dimensions of (rectangular) Cross Section

H = Height perpendicular to gripping dies, inches
B = Breadth parallel to gripping dies, inches

[2] Rietsch, E. *Maschinenbau*, 16, 25–28 (Jan. 1937).

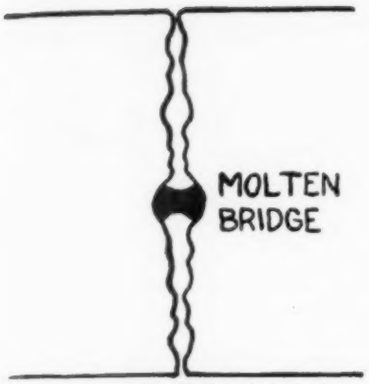


Fig. 3—Explanation of the Flashing Mechanism in Flash Welding

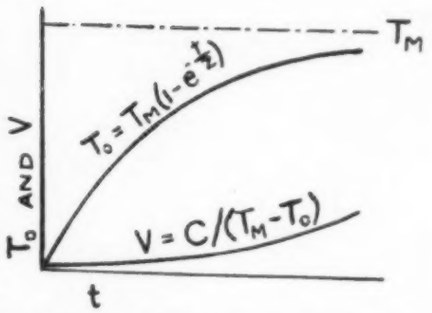


Fig. 4—Flashing (Push-Up) Velocity and Average Temperature of Surfaces to Be Welded During Flashing Time Starting Cold

T_o = initial temperature
T_m = temperature of molten metal
V = flashing (push-up) velocity
t = time of flashing

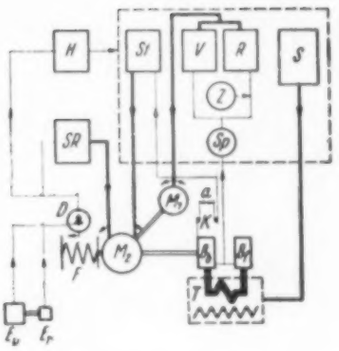


Fig. 5—Schematic Diagram for the Control of an Automatic Flash Welder

The liquid bridges tear themselves apart by surface tension, the resistance increasing as the cross section of liquid metal is thus decreased. The high temperature created by the increased resistance vaporizes the remainder of the bridge and gives rise to a new arc. Most of the surrounding liquid metal is ejected from the joint by the explosion corresponding to the arc formation. The surfaces to be welded retain a thin coating of liquid metal. Of course, many liquid bridges should form at once in the process of flash welding. For this reason surfaces to be flash welded should preferably be rough to increase the number of bridges and shorten the time of welding.

Flash Welding Calculations

In flash welding the weight of melted metal multiplied by its rise in temperature and specific heat must equal the power output of the machine. On this basis it is shown that

$$T_o = T_m - \frac{C_1 N}{VA}$$

where T_o = initial temperature of cold bar
T_m = temperature of molten metal
N = output of machine
V = flashing velocity = L/t
A = cross-sectional area of weld
C_1 = a constant
L = length lost in flashing
t = time of flashing

This equation shows that the smaller the temperature difference $T_m - T_o$ the greater is the flashing velocity. This is important for flash welding cold material. For many years cam-actuated single-purpose machines have been built for welding a definite cross section. In the past the shape of the cam has been determined experimentally, but it can now be calculated by means of the equation

$$T_o = T_m\,(1 - e^{-\frac{t}{z}})$$

where t is the variable time, and z is the "time constant" of the cross section to be welded. If such a heating curve is assumed, then the flashing velocity varies as shown in Fig. 4, which closely approximates experimental results.

Automatic Flash Welding Machines

In view of the effect of surface irregularity on arcing, it is not surprising that automatic flash welders cannot be so accurately regulated as automatic arc welding machines. The above formula for T_o shows that a fully automatic machine of wide range can be produced for flashing preheated material. The only requisite to be met is that the movable part of the machine is given a closely controlled velocity V. Since the cross section to be welded is given and the power of the machine is determined by the transformer taps, the formula for T_o states that flashing will occur of itself at a definite temperature T_o. If T_o is not yet reached the cross sections to be welded move into close contact with velocity V without flashing. In this case, if the time of contact is long enough, resistance heating suffices to raise the sections to T_o. On the next forward motion flashing begins because conditions for T_o are fulfilled.

Rietsch concludes his article with a schematic diagram Fig. 5 of an automatic flash welder and states that the maximum compact cross section for such welders appears to be about 75 to 80 sq. in. In Fig. 5, after closing switch E_v the relay H obtains voltage and either switches in the entire set of relays inside the dotted line or prepares for such switching. Motor M_1 is turned on in a forward direction by switch V. The bars to be welded are gripped in the immovable die B_f and the movable die B_b, and move toward each other with the velocity imparted by motor M_1. If the bars are cold and of compact cross-section then on contact there is quiet heating without flashing. The voltage across the dies greatly decreases and voltage relay S_p switches out relay V. Motor M_1 comes to a stop. An interval of time regulated by time relay Z is allowed to pass after which relay R comes into action, switching in motor M_1 in reverse. The bars move apart until voltage relay S_p is actuated by the sudden increase of voltage across the dies when the bars separate. Without delay relay R is switched out and V switched in. If the time of preheating was sufficient then initial temperature T_o for flashing is reached. On the bars coming into contact again flashing occurs during which the voltage does not change much and voltage relay S_p does not come into operation. Motor M_1 therefore keeps moving forward until the flashed length A has been traversed by the movable grip B_b. At this point contact K actuates the push-up limit S_l and switches in the fast motor M_2. In this way the bars are butted violently together until a critical pressure is attained. When the critical pressure is reached, spring F brings the pressure release D into action whereupon all relays go out of action, and the welding is complete. E_r is the switch for relay SR which rapidly brings the movable die to the initial position. S is the switch for the welding current and T the welding transformer.

Reitsch also states that American practice is toward the use of cam-actuated machines, whereas in Germany the trend is toward the preheater type which avoids the extremely slow and uneconomical push-up velocities required by cam-actuated machines.

An Investigation of Plug and Slot Welds*

By GLENN JAMES GIBSON†

Introduction

PLUG welding is a method of joining two members where a hole in one member is filled with weld metal at the same time fusing into the other member. When the hole is other than round the weld is called a slot weld. The general practice at present is to avoid plug welds whenever possible, not only because they are not as economical as fillet welds, but also because there has been considerable doubt and distrust as to their strength and reliability. At the present time plug welds are permissible under the American Petroleum Institute-American Society of Mechanical Engineers Code at very conservative figures, which were based on a limited number of tests** made in England in 1929. The purpose of this investigation is to determine the design, welding procedure and reliability of the strength of plug and slot welds.

Plug welds have applications where they can be used at an advantage over other types of welds. In a fillet-welded joint where there is not enough available space to develop the strength of the member by fillet welds, plug welds can be used to provide additional strength. Plug welds can also be used in place of overhead fillets in such cases as in battledeck bridge flooring and in strengthening plate girders by welding on additional cover plates. Fillet welded lap type pipe joints which can be welded from the outside only can be stiffened considerably by the addition of a series of plug welds around the lap acting with the fillet. Plug welds also afford a convenient way of attaching additions to the existing buildings. Some form of plug welding is necessary where a watertight welded plate covering is attached to a narrow frame such as in the rudder of a ship where most of the welding must be done from outside. One important property of plug welds is that the distortions of the member due to the shrinkage of the weld metal seems to be practically negligible. This property has its important application in plate work where plate distortions due to fillet welding has limited the application of welding in plate fabrication.

The present literature on the subject of plug welds indicates an uncertainty and distrust in this type of welding probably because of the scarcity of experimental data on the subject. Prior to 1930 the tendency was to fill up the holes completely and base the strength of the plug on its shear area which was assumed equal to the area of the hole. Since that time however, it has been contended that the weld metal could not penetrate into the periphery of the hole. Consequently the diameter of the actual shear area should be less than the diameter of the hole. The recent tendency is to make the diameter of the holes greater than twice the plate thickness and to build a fillet around inside the hole, the strength of which would be determined by the length or circumference of its center of gravity. This idea seems to have come from the effort to relate everything in terms of fillet welds and also from the fear that the weld metal might crack due to the shrinkage of a large mass of weld metal in a filled hole. The two design theories suggested for the above types are as follows:

A. P. I.—A. S. M. E. Code.

Filled Plugs

$$P_s = 0.63\ (D\text{-}{}^1/_4)^2\ f_s$$

Fillet Plugs

$$P_s = \pi\ (D\text{-}{}^2/_3 t)\ f_s\ 0.707\ t\ 0.909$$

(based on the AMERICAN WELDING SOCIETY shear values)
where: P_s = allowable load on plug welds in shear—pounds

D = diameter of hole—inches

t = thickness of plate and size of the weld (fillet size made equal to thickness of the plate)—inches

f_s = allowable shear unit stress—pounds per square inch
A. W. S. value is 11,300 psi.

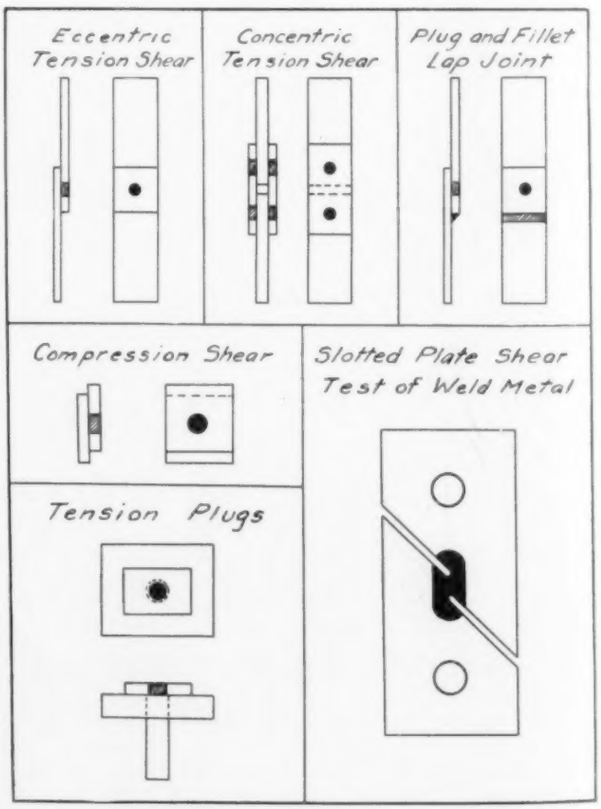


Fig. 1—Types of Test Specimens

* Presented as a Thesis for M.S. degree in Civil Engineering, Lehigh University, Bethlehem, Pennsylvania.
Contribution to Fundamental Research Committee.

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** British Engine, Boiler, and Electrical Insurance Company, Technical Report 1929, 101–105.

The factor "0.63" is $\frac{\pi}{4} \times 0.80$, it being assumed that the weld will be eighty per cent efficient.

The factor 0.909 represents the average value of a fillet weld on a circle; i.e., the average of the fillet one hundred per cent efficient at right angles to the direction of loading and with the fillet seventy-five per cent efficient parallel to the direction of loading. This factor enters the equation because of the experimental fact that fillet welds parallel to the direction of loading are only seventy-five per cent as efficient as fillet welds transverse to the direction of loading.

Test Program (Fig. 1)

A. Effect of the Type of Test Specimen

1. Eccentric tension shear test
2. Concentric tension shear test
3. Compression shear test (Fig. 2)

B. Effect of Type of Weld

1. Filled plugs
2. Fillet around hole

C. Relation Between Size of Hole and Thickness of Plate

1. For weldability
2. Strength and reliability
3. For economy

D. Relation Between Depth of Plug and Size of Hole

1. In compression shear
2. In eccentric tension shear

E. Effect of Different Brands and Types of Electrodes on the Strength of Plug Welds

1. Compression shear tests
   (*a*) Simple plugs
   (*b*) Continuous plugs
2. Shear tests of weld metal in slotted plate specimens

F. Plug Welds Made in the Vertical Position

G. Effect of Current on the Strength and Weldability

H. Plug Weld Tension Tests

I. Compression Shear Tests of Slot Welds

1. Load deflection curves of longitudinal and transverse filled slot welds (Fig. 3)

J. Plug and Fillet Lap Joint Tests

1. Strength added by plugs
   (*a*) Spacing of plugs

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Preparation of Specimens and Welding Procedure

The holes in all the specimens were drilled in the shop. The slots were drilled and chipped so the desired size would be known with accuracy. The plates for the tension specimens were sawed from universal rolled bars and no further machining was necessary. The plates used in the compression shear tests had the inside corners rounded to avoid digging of the plates as they slid over each other during testing. The ends were faced to insure a square bearing on the shear testing rig and on the head of the testing machine. The slotted shear test specimens were cut from 9 by 4 by $^3/_8$-in. plates. First the two holes were drilled for the loading pins, and then the one-inch wide slot was cut in the center and filled with weld metal. The backing-up strip was then faced off and the two $^1/_8$-in. 45° slots were cut into $^1/_8$-in. holes drilled on the center line. This specimen provides a shear area of weld metal $^3/_8$ by $^1/_2$ in.

A special welding procedure is necessary to insure good fusion and uniformity of plug welds. All of the welds were made by the writer who had the assistance of Mr. Keyser in establishing the welding procedure. Unlike straight fillet welding, the direction of the welding is constantly changing as the beads are built up around the hole. In flat welding the first pass is made around the edge to fill up the corner between the edge of the hole and the plate. The welding electrode should be inclined about 30 degrees from the vertical and about the normal amount against the direction of welding. The speed of travel should be just fast enough to keep the slag from getting ahead of the arc. The circular direction of the weld and the inclination of the rod require considerable manipulation of the holder to keep the rod always pointed in the correct direction. The holder must be manipulated in a circle, the diameter of which depends on the length

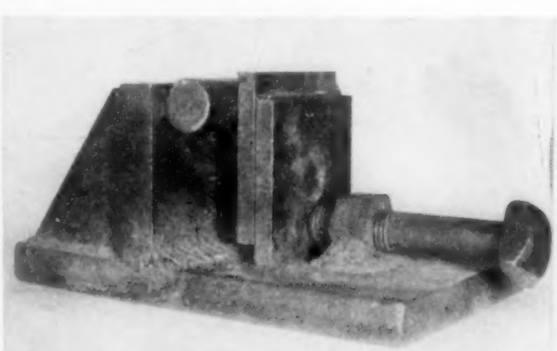


Fig. 2—Compression Shear Testing Rig

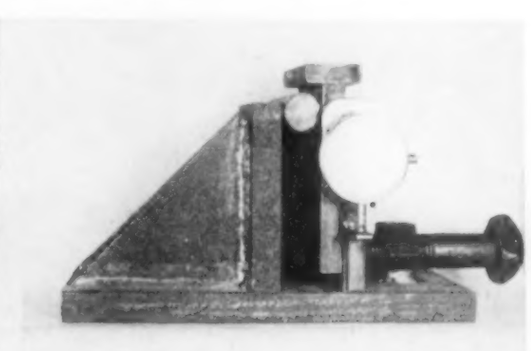


Fig. 3—Compression Shear Testing Rig For Load Deflection Readings

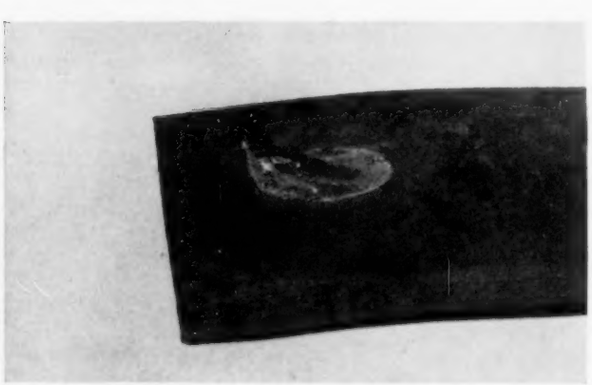


Fig. 4 (Top)—A Fillet Type Plug Weld

Fig. 5 (Bottom)—A Typical Fracture of a Large Plug Weld Showing a Tension Failure on the Side of the Hole and the Diagonal Shear Through the Plug

of the rod. Rods over twelve inches long are very difficult to handle. This technique is necessary to insure full corner fusion, and to make the area of the weld metal equal to the area of the hole. In small holes, three-quarters of an inch or less in diameter, the weld metal is deposited around the hole so quickly that it is very difficult to properly manipulate the rod, and the resulting plugs are not satisfactory. Another factor that enters is the relation between the size of the hole and the thickness of the plate. This relation must be such that the welding rod can be properly inclined without touching the top edge of the hole. The one severe economic handicap of plug welds is the necessity of chipping and knocking out the slag after each pass. The suggestion has been made of using smaller holes and welding all in one operation by allowing the molten slag to be floated out. This idea was tried but with unsatisfactory results. The weld metal builds up like a column in the middle of the hole with the slag freezing around the edges, preventing all fusion between the two plates. In addition to chipping, the slag must be removed from the hole before further welding can proceed. In making small specimens in the laboratory it was a simple matter to turn them over so the slag fell out, but it is obviously impossible to turn a structure over after every pass of welding. To make plug welds successful commercially it seems necessary for a welder to work on a series of holes at one time and to be followed by a helper who chips the slag and blows it out of the holes with an air-hose.

In making a filled plug weld a $^{3}/_{16}$ heavy coated electrode using the maximum range of recommended current was most satisfactory in securing the best speed and fusion. After the first pass around the corner between edges of the hole and plate, the center portion is filled up by the ordinary weaving motion for flat welding. If the rod does not have too heavy a coating and enough heat is used to float the edges of the deposited slag, these two operations may possibly be performed continuously. This practice should not be attempted if the holes are large or until the welder is sure that he is not trapping any slag. These two passes will build up the plug about $^{1}/_{8}$ in. and similar succeeding series of passes will fill the hole the required amount. The appearance of the finished weld can easily be made very good, but it does not follow that the weld is necessarily of standard quality, because the most important part of the weld (the first two passes) are entirely covered up.

The hole for making a fillet type plug weld should be somewhat larger than for the hole of the filled plug weld. If the weld is going to be a distinctly fillet type the hole should be large enough so that the lower legs of the fillet do not overlap one another. A smaller rod and less current are necessary than for the filled plugs to keep the weld metal from running down into the center because the heat is concentrated on a small area. Since the fillets are built up in a series of small beads many more passes will be necessary than required for the filled type. This procedure greatly increases the unit cost of the deposited weld metal over that of the filled plugs for which the larger electrodes are used. The fillet plugs have a rough irregular contour and in general give a decidedly unfinished appearance which might have a bad psychological effect (Fig. 4). It has been said that these cavities in fillet plug welds look so bad that they should be filled with a paste filler to improve their appearance.

Welding plugs in the vertical position offers many new difficulties in welding procedure. It has been suggested that all plugs in the vertical position should be welded by weaving a heavy bead back and forth up the face of the plug, allowing all the slag to run out and thus avoid the necessity of stopping to chip and blow out the slag.

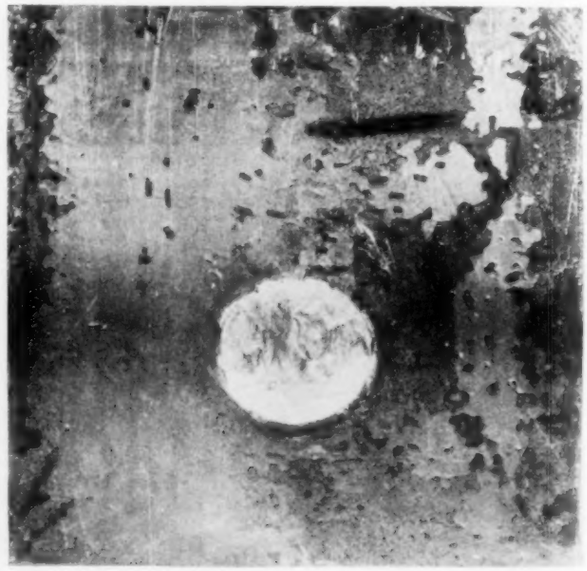


Fig. 6—A Face Shear Failure of a Compression Shear Specimen

This method seems to work fairly well until the top of the hole is reached. At this point it becomes impossible to get fusion within about 1/8 in. of the top of the hole on the face of the plate because the rod cannot be held close enough and the intense concentration of the heat causes the weld metal to run out of the closing crack. A better practice for electrodes with the heavier coatings is to start in the bottom corner and build a bead along the corners up to the top on both sides, chip the slag and then weave the center, filling the plug out in passes just as the flat plugs are built up. A 5/32 universal coated welding rod with direct polarity and average current for vertical welding are recommended. The vertical plugs have the usual ragged appearance of vertical welds made with heavy coated electrodes. Another much more economical procedure for vertical plugs is possible by using the new type universal electrodes with lighter coatings than the old type. A 5/32 rod with direct polarity using a much higher current than ordinarily used for vertical welding is recommended for the following procedure. Start at the top and weld down the corners on each side and then weave up the center similarly to ordinary vertical welding. In welding down it is necessary to travel fast enough to prevent the slag from running ahead and shorting the arc. The high current is necessary to force the weld metal in the upper corner and to melt the deposited slag causing it to run out, thus avoiding slag inclusions. It is only necesary to chip the slag when the welder has to stop and change electrodes. The last pass should be woven with less current and should be carried outside the area of the hole to insure a smooth finish and a water-tight joint.

The procedure used in welding the plug weld tension specimens was the same as used for flat plugs. Instead of the weld being fused to another plate it is welded on the end of a 1 1/2 in. round bar as shown in Fig. 1.

Testing Procedure

The eccentric and concentric tension shear and the tension plugs were pulled in the 300,000-lb. capacity Olsen testing machine. The ultimate load was recorded and the fracture was examined to determine the type of failure and the amount of fusion. The plug and fillet lap joints were whitewashed to facilitate the observations of the type of failure and the yield point of the material. They were also tested in the 300,000-lb. Olsen machine. The compression shear specimens were tested in special testing rigs (Fig. 2) using the 50,000-lb. Riehle machine for the small specimens and the 300,000-lb. Olsen for the larger ones. The ultimate load and a description of the fracture was noted in all cases. Load deflection curves for the compression shear slot welds and a few of the larger plug welds were obtained by 1/10,000 in. Ames dials attached as shown in Fig. 3. The slotted plate shear specimens were tested in the 50,000-lb. Riehle machine with the use of special clevises.

Discussion of Results

A. Effect of type of Test Specimens.—A description of the specimens and results are recorded in Table A. This series of tests served as a preliminary study and formed a basis for the remainder of the program. Three different types of specimens were used: (1) eccentric tension shear, (2) concentric tension shear and (3) compression shear (Fig. 1) to determine the simplest and most economical type which would give the most practical results. The other variables studied in this series were the size of hole and the type of weld.

In the eccentric tension shear tests the plates are bent into line by the action of the direct pull. This results in a tension or pullout component on the plug which reduces the ultimate shear stress as compared with the two other types of specimens. All except the 1 3/4-in. hole specimens failed in face shear and the plugs were found to be completely fused to the sides of the hole and the base plate except where noted in the table. Face shear is the shearing action that takes place along the

Table A—Effect of Type of Test Specimens

Electrode	Hole Diam. In.	Test Plate In.	Plug Depth In.	Ultimate Load Lb.	Shear Stress psi	Remarks
Eccentric Tension Shear Tests						
3/16 A	1	3 × 1/2	1/2	30,800	39,200	Fusion good. 1/8-inch blow-hole in weld metal
3/16 A	1	3 × 1/2	1/2	30,450	38,800	Fusion poor on tension side of hole
3/16 A	1	3 × 1/2	1/2	30,850	39,300	3/16-inch hole in center of weld metal
3/16 A	1 3/16	4 × 1/2	1/2	38,600	34,900	Fractured 1/8 inch into base plate. Fibrous break
3/16 A	1 3/16	4 × 1/2	1/2	41,050	37,100	Not fused 1/16 inch on side of hole
3/16 A	1 3/16	4 × 1/2	1/2	42,400	38,400	Not fused 1/16 inch on side of hole
3/16 A	1 3/4	6 × 1/2	1/2	80,000	33,300	Diagonal fracture through plug with tension failure on side. Fusion good
5/32 B	1 3/4	6 × 1/2	1/2 F*	79,500	40,300	
Concentric Tension Shear Tests						
3/16 A	1	3 × 1/2	1/2	66,500	42,400	No fusion, 1/16 inch on side of hole
3/16 A	1	3 × 1/2	1/2	63,900	40,800	Not fused 1/16 inch on side of hole
3/16 A	1 3/16	4 × 1/2	1/2	84,500	38,100	One plug failed before the other
3/16 A	1 3/16	4 × 1/2	1/2	90,200	40,800	Good fusion
5/32 B	1 3/4	6 × 1/2	1/2 F	160,900	40,800	Welds slightly undersize
5/32 B	1 3/4	6 × 1/2	1/2 F	173,250	44,100	Good fusion
Compression Shear Tests						
3/16 A	1	3 × 1/2	1/2	32,900	41,900	Fair fusion on side of hole
3/16 A	1	3 × 1/2	1/2	33,000	42,100	3/16-inch blow-hole in center
3/16 A	1	3 × 1/2	1/2	34,600	44,200	Complete fusion
3/16 A	1 3/16	4 × 1/2	1/2	49,170	44,400	Complete fusion
3/16 A	1 3/16	4 × 1/2	1/2	48,750	44,100	Complete fusion
3/16 A	1 3/4	6 × 1/2	1/2	92,000	38,200	Poor fusion 1/16 inch on side of hole
5/32 B	1 3/4	6 × 1/2	1/2 F	89,500	45,500	Some digging of plates. Complete fusion
5/32 D	1 3/4	6 × 1/2	1/2 F	94,600	48,200	Plate started to buckle
5/32 B	1 3/4	6 × 1/2	1/2 F	97,750	49,600	Plate started to buckle

* F = Fillet.

Fillet shear area taken as $= \frac{\pi}{4} D^2 - \frac{\pi}{4}(D - 2F)^2$.

Table B—Effect of Type of Weld

Electrode	Type of Specimen	Hole Diameter In.	Plug Depth In.	Ultimate Load Lb.	Shear Stress on Nominal Fused Area psi	Load per* Lineal Inch of Fillet Welds Lb.	Remarks
5/32 *B*	E.T.S.	1 3/4	1/2 *F*	66,400	33,700	14,900	Failed in tension on side of hole, and sheared diagonally through plug
3/16 *A*	E.T.S.	1 3/4	1/2	80,000	33,300		
5/32 *B*	E.T.S.	1 1/2	1/2 *F*	72,700	46,200	19,700	Weld oversize
3/16 *A*	E.T.S.	1 1/2	1/2	69,400	38,700		Face shear failure
5/32 *B*	E.T.S.	1 1/4 × 2 1/2 slot° longitudinal	1/2 *F*	79,700	32,800	14,700	Failed in tension on end of slot
3/16 *A*	E.T.S.	1 1/4 × 2 1/2 slot longitudinal	1/2	86,700	31,000		Failed in tension on end of slot
5/32 *B*	E.T.S.	1 1/4 × 2 1/2 slot transverse	1/2 *F*	76,400	31,400	14,200	Tension failure on side and shear through throat
3/16 *A*	E.T.S.	1 1/4 × 2 1/2 slot transverse	1/2	86,900	31,100		Fractured through base plate
5/32 *B*	C.S.	1 1/4 × 2 1/2 slot longitudinal	1/2 *F*	92,900	38,200	17,200	Poor fusion 1/8 inch on side of tension end of slot
3/16 *A*	C.S.	1 1/4 × 2 1/2 slot longitudinal	1/2	113,000	40,400		Complete fusion
5/32 *B*	C.S.	1 1/4 × 2 1/2 slot transverse	1/2 *F*	110,900	45,700	20,600	Complete fusion. Fillet oversize
3/16 *A*	C.S.	1 1/4 × 2 1/2 slot transverse	1/2	117,000	41,900		Complete fusion

* Length of fillet based on the length of its center of gravity.
° Ends of slots semi-circular with a radius of 5/8 inch.

plane between the two plates. The shear area of the filled plugs was assumed equal to the gross area of the hole, and the average shear strength computed on this basis was 38,500 psi. The specimens with 1 3/4-in. holes did not fail in face shear but broke in tension from the side of the hole and sheared diagonally through the plug. The plugs transmit their shear load to the plate not only by bearing on one side of the hole similar to the action of rivets, but also by tension on the other side because the plug is fused to all sides of the hole. It is the bearing which causes the diagonal fracture of the plug after it has broken from the tension side of the hole. This indicates that there may be a limiting size of hole for each thickness of plate. The shear stress of the fillet type plug was based on the area of the fillet fused into the base plate. This is apparently a correct assumption when the fillets fail in face shear, but the 1 3/4-in. hole was so large that the plug failed in tension on the side of the hole and sheared diagonally through the plug, similar to the failure of the specimen shown in Fig. 5.

In the concentric tension shear tests the filled type plugs failed in face shear at an average shear strength of 41,000 psi. Figure 6 shows a specimen which fractured in face shear. The fusion can be seen to be uniform and complete over the entire cross section of the hole. The shear values for fillet plugs are not as uniform as the filled type because of the difficulty in controlling the size of the weld.

The compression shear specimens failed in face shear at an average shear strength of 44,000 psi. This value is somewhat higher than later tests of this same type which average 41,800 psi. The variation might be blamed on friction between the two plates since all projecting corners and edges were ground off the plates in subsequent tests. The 1 3/4-in. filled plug had a lower shearing strength which is probably due to progressive failure probably taking place as indicated by the oval shape of the fractured plug shown in Fig. 6. The fillet type specimens do not show their true shear value because the test plates buckeled somewhat during the test which caused bearing friction, and as a result all the load was not carried in shear by the plug. The average maximum compressive stress in these plates was 32,000 psi, but the secondary stresses due to the eccentricity probably caused the buckling. This shows that the plates should be made thick enough to avoid buckling. The general indication from this series is as follows:

1. Complete face fusion is possible, therefore the strength of plugs should be based on the shear of the gross area of the hole.

2. There is a maximum ratio of the diameter of the hole to the thickness of the plate to insure face shear failure in eccentric tension.

3. Compression shear specimens can satisfactorily replace the concentric tension shear type because they are more economical and they more clearly indicate the variations between specimens.

B. Effect of Type of Weld—Table B.—The fillet plug and the filled plug are confusing because of the similarity in their names. A fillet plug consists of building a fillet around the circumference of the hole between the sides of the hole and the base plate, while a filled plug is simply the solid filling of the hole with weld metal fusing into the sides of the hole and to the base plate. The shear strengths of the filled and fillet type compare favorably on the basis of the welded area on the base plate. The first two specimens of the Table B did not develop the expected shear strength of the plug because the hole was too large for the plate thickness. Under these conditions the plug failed in tension on the side of the hole and sheared diagonally through the plug (Fig. 5). The strength of the fillet plugs is more variable because their size is difficult to control. In a tension qualification test 1/2-in. fillets usually fail at about 18,000 lb. per lineal inch, while the fillets in plugs and slot welds show a wide variation from this strength.

The filled and fillet welded slot specimens came far from developing the shear strength of the welded area in the eccentric tension shear tests. The concentration of tension on the ends of the longitudinal slots was the primary cause of failure of this type. The plate was not rigid enough to evenly distribute the load to the two sides of the transverse slots and as a result one side of the weld failed before the other side developed its ultimate strength. This progressive type of failure is the cause of the low strength values of slot welds. If the weld is too

large for the thickness of the plate, the section of the base plate fused to the plug is likely to tear out. The transverse filled slot specimen failed in this manner. Transverse slot welds have their longer axis perpendicular to the direction of loading, while longitudinal slots have their longer axis parallel to the direction of loading.

This progressive failure was avoided in the compression shear tests because $^3/_4$-in. plates were used with the same slot and weld sizes as before. Transverse slot specimens of the size tested show little advantage over the longitudinal type. Transverse slots have the disadvantage of causing a greater reduction in the net section of the plate when they are used in combination with other welds. Slots and large holes should be avoided in any use which involves tension in the connected members because of this dangerous tendency toward progressive failure.

The usual objection to filled plugs has been the idea that the weld would crack due to shrinkage of the concentrated mass of weld in the plug. This belief is a survival of the days when plugs which were made of bare wire, probably cracked and thus gave plug welding a bad reputation. In a study of this problem, two $1^1/_2$-in. holes in $^3/_4$-in. plates were filled with weld metal, one with bare wire and one with a modern coated electrode. The welds were faced off, roughly polished and etched in boiling 6N hydrochloric acid for fifteen minutes. The weld made by the coated electrode showed no signs of cracking while the bare wire weld had cracked half way around the edge of the hole. The weld metal of the covered electrodes was ductile enough to allow plastic flow to partially relieve the shrinkage stresses. Later tests of pure tension plugs demonstrated that no cracking was present in covered wire welds. This proves that bare wire plugs should be avoided and there is no danger of cracking with weld metal from good coated electrodes.

Table C—Effect of Size of Hole and Thickness of Plate

Compression Shear Tests

Hole Diam. *D* In.	Test Plate In.	Plug Depth *P* In.	Ratio *D*/*P*	Ultimate Load Lb.	Shear Stress psi	Remarks
$^1/_2$	4 × $^1/_2$	$^1/_2$	1.0	6,600	33,600	Poor fusion on sides and bottom of hole
$^3/_4$	4 × $^1/_2$	$^1/_2$	1.5	17,200	38,900	Poor fusion $^1/_{16}$ inch on side and bottom of hole
$^3/_4$	4 × $^1/_2$	$^1/_2$	1.5	16,900	38,300	Poor fusion $^9/_{16}$ inch on side and bottom of hole
1	4 × $^1/_2$	$^1/_2$	2.0	31,060	39,500	Poor fusion $^1/_{16}$ inch on side of hole
1	4 × $^1/_2$	$^1/_2$	2.0	31,550	40,100	Fair fusion $^1/_{16}$ inch on side of hole
$1^3/_{16}$	4 × $^1/_2$	$^1/_2$	2.4	46,300	41,800	Average of a number
$1^3/_4$	6 × $^1/_2$	$^1/_2$	3.5	92,000	38,200	Poor fusion $^1/_{16}$ inch on side of hole
$^3/_4$	3 × $^1/_4$	$^1/_4$	3.0	17,170	38,800	Fusion on bottom but not $^1/_8$ inch on side of hole
$^7/_8$	3 × $^1/_4$	$^1/_4$	3.5	22,850	38,000	No fusion $^1/_{16}$ inch on side of hole
$1^1/_8$	4 × $^3/_8$	$^3/_8$	3.0	39,800	40,100	Poor fusion $^1/_{16}$ inch on side of hole
$1^1/_8$	4 × $^3/_8$	$^3/_8$	3.0	41,300	41,600	Good fusion
$1^3/_{16}$	4 × $^1/_2$	$^1/_2$	2.4	46,300	41,800	Average of a number
$1^1/_4$	4 × $^5/_8$	$^5/_8$	2.0	52,100	42,500	Poor fusion on side of hole in spots
$1^1/_4$	4 × $^5/_8$	$^5/_8$	2.0	50,600	41,300	Poor fusion $^1/_8$ inch on side of hole
$1^1/_2$	6 × $^3/_4$	$^1/_2$	3.0	78,100	44,300	Complete fusion
$1^1/_2$	6 × $^3/_4$	$^1/_2$	3.0	78,800	44,600	Complete fusion
$1^3/_4$	1 × 6	$^5/_8$	2.8	99,300	41,300	Complete fusion
$1^3/_4$	1 × 6	$^5/_8$	2.8	94,000	39,200	Good fusion. Weld metal slightly porous

$^3/_{16}$ *A* Electrode used for all welds.

Table D—Relation Between Depth of Plug and Size of Hole

Compression Shear Tests

Hole Diam. *D* In.	Test Plate In.	Plug Depth *P* In.	Ratio D/*P*	Ultimate Load Lb.	Shear Stress psi	Remarks
$^{15}/_{16}$	3 × $^3/_8$	$^3/_8$	2.5	27,500	39,800	Poor fusion $^1/_{16}$ inch on side of hole
$^{15}/_{16}$	3 × $^3/_8$	$^1/_4$	3.7	27,700	40,100	Poor fusion $^1/_{16}$ inch on side of hole
$^{15}/_{16}$	3 × $^3/_8$	$^3/_{16}$	5.0	28,700	41,500	Poor fusion $^1/_{16}$ inch on side of hole. Fractured diagonally through plug
$1^1/_8$	4 × $^3/_8$	$^3/_8$	3.0	39,800	40,100	Poor fusion $^1/_{16}$ inch on side of hole
$1^1/_8$	4 × $^3/_8$	$^3/_8$	3.0	41,300	41,600	Good fusion
$1^1/_8$	4 × $^3/_8$	$^3/_{16}$	6.0	38,500	38,800	Tension failure on side of hole
$1^1/_8$	4 × $^3/_8$	$^3/_{16}$	6.0	39,520	39,700	Tension failure on side of hole
$1^1/_8$	4 × $^3/_8$	$^1/_8$	9.0	23,000	23,100	Fractured diagonally through plug
$1^1/_8$	4 × $^3/_8$	$^1/_8$	9.0	32,500	32,700	Fractured diagonally through plug
$1^3/_{16}$	4 × $^1/_2$	$^1/_2$	2.4	45,000	40,600	Good fusion
$1^3/_{16}$	4 × $^1/_2$	$^5/_{16}$	3.8	43,160	39,100	Poor fusion on side of hole in spots
$1^3/_{16}$	4 × $^1/_2$	$^3/_{16}$	6.3	45,500	41,200	Good fusion
$1^3/_{16}$	4 × $^1/_2$	$^1/_8$	9.5	24,400	22,100	Fractured diagonally through plug
$1^1/_4$	4 × $^5/_8$	$^5/_8$	2.0	50,600	41,300	Poor fusion $^1/_8$ inch on side of hole
$1^1/_4$	4 × $^5/_8$	$^1/_2$	2.5	51,000	41,600	Poor fusion $^1/_{16}$ inch on side of hole
$1^1/_4$	4 × $^5/_8$	$^3/_8$	3.3	49,900	40,700	Poor fusion $^1/_8$ inch on side of hole
$1^1/_4$	4 × $^5/_8$	$^1/_4$	5.0	45,300	37,000	Plug broke in tension from side
				Eccentric Tension Shear		
$^7/_8$	4 × $^3/_8$	$^7/_{16}$*	2.33	23,400	38,900	Complete fusion
1	4 × $^3/_8$	$^7/_{16}$*	2.67	31,200	39,800	Complete fusion
$1^1/_{16}$	4 × $^3/_8$	$^7/_{16}$*	2.83	35,700	40,300	Complete fusion
$1^1/_8$	4 × $^3/_8$	$^7/_{16}$*	3.00	37,500	37,900	Complete fusion
$1^1/_4$	4 × $^3/_8$	$^7/_{16}$*	3.33	42,000	34,200	Complete fusion. Small tear on tension side of hole
$1^3/_8$	4 × $^3/_8$	$^7/_{16}$*	3.67	46,400	31,400	Complete fusion. Failed on tension side of hole

$^3/_{16}$ *A* Electrodes used for all welds.
* Plugs capped $^1/_{16}$ inch.

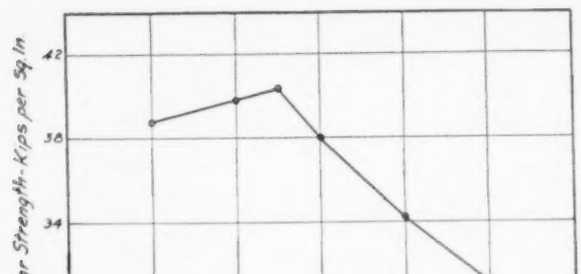


Fig. 7.—Effect of Diameter of Hole on Efficiency of Plug Welds in Eccentric Tension Shear

Although fillet plugs use less weld metal, their unit shear strength compares well with filled plugs. However, since the other advantages of filled plugs seem to make them more desirable, the remainder of the investigation is confined to the filled type plug. In reviewing the advantages of filled plugs we find that they require less time to make because larger electrodes with high current can be used. This procedure requires fewer passes and less slag chipping, and the greater heat produces better fusion and reliability. The design is simpler and the shear strength is more uniform because the size of the weld is limited to the size of the hole. Smaller holes can be used than those required by fillet welding. This is important in welding thin plates where it is desirable to maintain a maximum net section of the plate. Finally, filled plugs have much better appearance than the fillet type of plugs.

The conclusions from this series are:

1. The filled type of plug weld has many advantages over the fillet type.
2. There is no danger of the filled plug cracking due to shrinkage when made with modern heavy coated electrodes.
3. Large plugs and slots fractured at reduced shear values because of a tendency toward progressive failure.

C. Relation Between Size of Hole and Thickness of Plate.

D. Relation Between Depth of Plug and Size of Hole.

Tables C and D.—These two topics are so closely related that they should be discussed together. The object is to determine the most economical ratio between the size of hole to the thickness of the plate (D/T) or between the size of hole to the depth of plug (D/P). The latter is synonomous with the former ratio when the hole is completely filled. The minimum of this ratio is limited by the minimum diameter of hole in a given thickness of plate that can be successfully welded. The maximum of the ratio is limited by the maximum diameter of hole for a given thickness of plate that can develop the full shear strength of the plug by insuring face shear failure.

The primary consideration for economy is to obtain the maximum strength per pound of weld metal. The secondary consideration is the cost of making the holes which could be made by punching, punching and reaming, drilling and possibly by flame cutting. Tests were made on drilled holes only, but punched holes would probably be satisfactory if the plates laid flat on each other. Since the economy of weld metal increases with the D/T ratio, providing the plug fails in face shear, our problem is to find the limiting maximum ratio of D/T.

The action of plug welds is considerably different than the action of rivets. Rivets transmit their shear load to the plate by bearing on the side of the rivet hole. Since plug welds are fused on all sides of the hole, the stress would be transmitted to the plate by bearing on one side of the hole and tension on the other side. However, when the plate yields across the plug section all of the shear load seems to be thrown to the tension side of the hole. This theory is based on the fact that in the eccentric tension shear tests, the initial failure is always at this point when the D/T ratio is too large. Since eccentric tension shear is the worst possible condition, all plugs should be designed accordingly. The problem reduces itself to the determination of the D/T ratio which will provide enough area on one-half the side of the hole to develop in tension the shear strength of the plug. This side area is further limited by the uncertainty of securing fusion around the lower edge of the hole within $^1/_{16}$ in. of the base plate.

The following theory is proposed to determine the maximum size hole for a given plate thickness when the hole is completely filled. Shear strength of plug = tension strength on one-half the side of the hole,

$$\frac{\pi}{4} D^2\, 40{,}000 = \frac{\pi D}{2}\left(T - \frac{1}{16}\right) 60{,}000$$

which reduces to:

$$D = 3\left(T - \frac{1}{16}\right)$$

where:

D = diameter of hole
T = thickness of plate (or plug depth)
40,000 = average shear strength of plug
60,000 = average tension strength of weld metal
$\frac{1}{16}$ in. = reduction in tension area due to uncertainty of fusion

This theory shows that the maximum D/T ratio should be something less than three.

The minimum diameter of hole for each thickness of plate that could be successfully welded was determined by tests recorded in Table C. The weldability or ability to get satisfactory fusion, limited the strength of plugs in $^1/_2$-in. plates until the ratio of D/T was increased to 2.4. A $1^3/_4$-inch hole with a D/T ratio of 3.5 did not develop the entire shear strength of the large area of weld metal because of a progressive shear failure. Various plate thicknesses were plug welded to determine the range of application of plug welds and the minimum diameter for weldability. The $^1/_4$-inch plates required a large ($D/T=3$) ratio for weldability and even then completely satisfactory results were not obtained. The minimum D/T ratio for weldability decreases as the plate thickness

Table D(a)—Recommended Limits of Hole Diameters for Plate Thickness

Plate Thickness or Plug Depth Inches	Minimum Hole Diameter for Weldability Inches	D/T Ratio	Maximum Hole Diameter to Develop Shear Strength of Plug $D = (T - {}^1/_{16})3$ Inches	D/T Ratio
$^1/_4$	$^3/_4$	3.0	$^9/_{16}$	2.2
$^5/_{16}$	$^7/_8$	2.8	$^3/_4$	2.4
$^3/_8$	1	2.8	$^{15}/_{16}$	2.5
$^7/_{16}$	$1^1/_{16}$	2.4	$1^1/_8$	2.6
$^1/_2$	$1^1/_8$	2.3	$1^5/_{16}$	2.6
$^9/_{16}$	$1^3/_{16}$	2.1	$1^1/_2$	2.7
$^5/_8$	$1^1/_4$	2.0	$1^{11}/_{16}$	2.7
$^3/_4$	$1^3/_8$	1.8	$2^1/_{16}$	2.7
1	$1^1/_2$	1.5	$2^{13}/_{16}$	2.8

is increased so that a 1-inch plate only required a $1^1/_2$-inch hole ($D/T = 1.5$). A larger hole seems inadvisable because the shear strength seems to fall off using a $1^3/_4$-in. hole because of a tendency toward progressive failure.

The series of tests recorded in Table D were made to determine the minimum ratio of D/P in order to insure face shear failure, where P is the depth of filling of the plug. In compression shear tests all specimens with a D/P ratio of 5 or greater did not develop the shear strength of the plug but failed by breaking in tension on the side of the hole and fracturing diagonally through the plug. The proposed limits of D/T ratio were designed for eccentric tension shear, and as a result the compression shear tests are on the conservative side. The recommended $^{15}/_{16}$ hole (Table D*a*) for $^3/_8$-in. plate was found to be too small for good weldability. A system of using a larger hole and capping the plug $^1/_{16}$ in. and extending it $^1/_8$ in. around the edges of the hole was employed to provide the additional fused area on the side of the hole. A series of eccentric tension shear specimens having various sized holes were made to check the theory. The results plotted in Fig. 7 show a decided peak of efficiency at a D/T ration of 2.83 which checks the theory very well and is on the conservative side. Although complete fusion was secured on all the plugs the unit shear strength increased up to a $1^1/_{16}$-in. hole probably because of a deeper average penetration. When the hole size was increased, the unit shear strength decreased rapidly because of the initial tension failure on the sides of the hole.

The conclusion of this series is summarized in Table D(*a*), in which are tabulated the minimum and maximum diameters of hole for each plate thickness, the minimum for weldability and the maximum capable of developing the shear strength of the plug. This table can also be used to determine the amount of filling of the hole (depth of plug) where D/T ratio is less than the maximum allowable.

E. Effect of Different Brands and Types of Electrodes on the Strength of Plug Welds—Table E.—The purpose of this series was to determine the variation in shear strength of plugs when various electrodes were used, and to determine the cause for the apparent lower shear strength of plugs in relation to shear strength of weld metal. All plug welds that failed in face shear did not fracture in the weld metal. The surface of the fracture extended between $^1/_{32}$ and $^1/_{16}$ inch into the base plate, which was probably the "burnt" zone of metal. This zone is an inherent characteristic of all welds. In fusion welding there is a gradation in the metallic structure from the cast weld metal to that section of the plate, at some small distance from the weld, which is not heated above its critical point. This is the result of the heating of the metal by the arc and its subsequent cooling. Low-carbon structural steel is not hardened by this tempering action, and it is not affected except in the zone where it is heated to a mushy state and the crystals are floating in a molten matrix. When the metal in this zone cools its physical properties of strength and ductility are considerably less than before, and it is known as "burnt steel."

The variation in the shearing strength of plug welds made with different electrodes extended from about 42,000 to 50,000 psi. These variations can be attributed to the quality of the weld metal and to the depth of penetration. The same current was used for all electrodes, but the electrodes with lighter coating probably pene-

Table E—Effect of Different Brands and Types of Electrodes on the Strength of Plug Welds

Compression Shear Tests

Electrode	Hole Diam. In.	Test Plate In.	Plug Filled In.	Ultimate Load Lb.	Shear Stress psi	Remarks
			Simple Plugs			
$^5/_{32}$ bare	$1^1/_8$	$3 \times {}^1/_2$	$^1/_2$	33,760	34,000	Porous weld and no fusion $^1/_4$ inch on side of hole
$^5/_{32}$ bare	$1^3/_{16}$	$3 \times {}^1/_2$	$^1/_2$	44,300	40,000	
$^3/_{16}$ *A*	$1^3/_{16}$	$4 \times {}^1/_2$	$^1/_2$	46,300	41,800	Average of a number
$^3/_{16}$ *B*	$1^3/_{16}$	$4 \times {}^1/_2$	$^1/_2$	46,310	41,800	Deep fracture in base plate
$^3/_{16}$ *B*	$1^3/_{16}$	$4 \times {}^1/_2$	$^1/_2$	46,080	41,700	Poor fusion $^1/_{16}$ inch on side of hole
$^3/_{16}$ *C*	$1^3/_{16}$	$4 \times {}^1/_2$	$^1/_2$	49,070	44,400	Perfect fusion
$^3/_{16}$ *C*	$1^3/_{16}$	$4 \times {}^1/_2$	$^1/_2$	48,500	43,900	Perfect fusion
$^3/_{16}$ *D*	$1^3/_{16}$	$4 \times {}^1/_2$	$^1/_2$	49,660	44,900	Good fusion
$^3/_{16}$ *D*	$1^3/_{16}$	$4 \times {}^1/_2$	$^1/_2$	51,870	46,800	Perfect fusion
$^3/_{16}$ *E*	$1^3/_{16}$	$4 \times {}^1/_2$	$^1/_2$	53,520	48,400	Good fusion
$^3/_{16}$ *E*	$1^3/_{16}$	$4 \times {}^1/_2$	$^1/_2$	55,900	50,500	Perfect fusion
			Continuous Plugs			
$^3/_{16}$ *A*	$1^3/_{16}$	$6 \times {}^1/_2$	1	49,600	44,800	$^1/_8$-inch hole in weld metal
$^5/_{32}$ *B*	$1^3/_{16}$	$6 \times {}^1/_2$	1	55,200	49,800	Complete fusion
$^3/_{16}$ *C*	$1^3/_{16}$	$6 \times {}^1/_2$	1	58,600	53,000	Complete fusion
$^3/_{16}$ *D*	$1^3/_{16}$	$6 \times {}^1/_2$	1	55,800	50,400	Weld metal somewhat porous
$^3/_{16}$ *E*	$1^3/_{16}$	$6 \times {}^1/_2$	1	57,800	52,200	Weld metal somewhat porous

Shear Test of Weld Metal in Slotted Plate Specimens

Electrode	Ultimate Load, Lb.	Ultimate Shear Stress, psi	Remarks
A	9,840	47,800	Nominal dimensions of shear area of $^1/_2 \times {}^3/_4$ were corrected for variations
A	10,320	50,400	
B	9,810	47,400	
C	10,276	53,700	
D	10,860	53,800	
E	10,170	53,000	

Description of Types of Electrodes

Type	Brand	Use	Slag Produced
A	1	Flat	Heavy and loose
B	1	Horizontal fillets	Heavy and loose
C	1	High strength 0.5 Mo	Heavy and loose
D	2	Universal	Light and tight
E	3	Universal	Light and tight
F	4	Universal	Very light and tight

Table F—Plug Welds Made in the Vertical Position

Compression Shear Tests

Electrode	Hole Diam. In.	Test Plate In.	Plug Filled In.	Ultimate Load Lb.	Shear Stress psi	Remarks
5/32 F	13/16	4 × 1/2	1/2	48,500	43,900	Complete fusion
5/32 F	13/16	4 × 1/2	1/2	48,400	43,800	Small slag inclusion in center
5/32 F	13/16	4 × 1/2	1/2	48,300	43,600	Weld metal somewhat porous
5/32 D	13/16	4 × 1/2	1/2	49,900	45,200	Weld metal somewhat porous

trated deeper than those producing a heavy slag. More heat would be needed to fuse a thick coating and less would be available for penetration. The deeper the penetration, the deeper would be the surface of "burnt steel" which would have a corresponding greater area and as a result would fracture at a higher load. The effect of depth of penetration could be shown much better in relation to the welding current. This method was tried in a later series of tests and the results substantiated the above conclusions.

To prevent the fracture of the plug in the burnt zone, a series of continuous plugs were made to insure the shear fracture of the weld metal. A continuous plug is made by drilling holes in both plates of the specimen and the weld metal is placed in both holes continuously. This corresponds to a riveted joint. The strength of all electrodes tested was increased and compares fairly well with the shear strength of the weld metal as determined by the slotted plate shear tests.

It is somewhat easier and faster to weld plugs in the flat position with an electrode whose slag does not freeze quickly. A loose slag also reduces the time required to chip it out. The difference in the strength of plug welds would probably be reduced when the appropriate currents are used for each. The various tests indicate that plug welds have the tendency to fracture in the burnt surface of the fusion zone and results in a low shear strength of plug welds.

F. Plug Welds Made in the Vertical Position—Table F.—Plug welds can be satisfactorily made in the vertical position, and their average strength is even higher than the average strength of horizontal plug welds. The higher strength can be attributed to the type of electrode, and the more intense heat than used in welding horizontal plugs. The continuous welding procedure using a light-coated universal electrode and the high current commonly used for overhead welding is necessary to produce complete fusion and uniform strength. The other described welding procedure was tried but no fusion could be produced in the upper corner of the hole because of the insufficient current to drive the weld metal in. Only those tests made with the continuous procedure are recorded in Table F. In filling the hole in the 1/2-inch plate, it was only necessary to chip the slag once and still there seemed to be little danger of slag inclusions. A smooth finish of the weld can be obtained if less heat is used on the last pass.

Plug welds might be more economically made in the vertical than in the horizontal position because they only require about one-half the chipping and no air hose would be needed to remove the slag chips.

G. Effect of Current on the Strength and Weldability—Table G.—The amount of current has a pronounced effect on the strength of plug welds. Using a 3/16 electrode the ultimate shear strength varied from 35,000 psi at 150 amperes to 50,000 psi at 300 amperes. The intermediate strengths were in proportion to the amount of current, as shown in Fig. 8.

This increase in strength with welding current substantiates the theory that the ultimate shear strength depends upon the amount of penetration described in Section *E*. The increased heat deepens and increases the area of the burnt surface of the fusion zone where the plug welds normally fail. The increased strength due to this effect will probably reach a maximum when the shear strength of the weld metal itself is reached. The maximum shear strength developed by Electrode *A* was 50,700 psi which was about the same as the corresponding values of ultimate shear strength as obtained on the slotted plate specimens. The 300 amperes current was considerably above the recommended upper limit for this 3/16 electrode, and at this current the rod melted so fast that no higher currents were used in this series of tests.

The amount of current has an important effect on the weldability of plug welds. Complete fusion on the base plate was secured in all cases, but up to 200 amperes it was difficult to secure fusion to the sides of the hole.

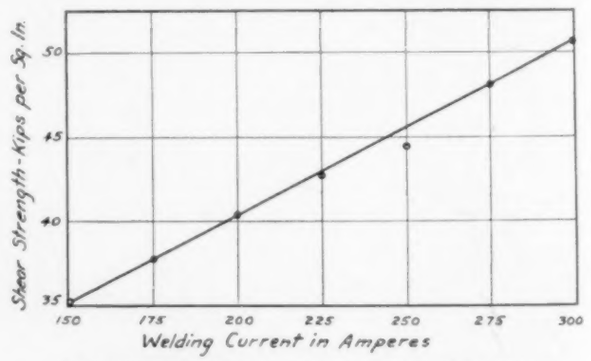


Fig. 8.—Effect of Welding Current on the Efficiency of Plug Welds

Table G—Effect of Current on Strength and Weldability

Compression Shear Tests

Electrode	Welding Current Amps.	Hole Diam. In.	Test Plate In.	Ultimate Load Lb.	Shear Stress psi	Remarks
3/16 A	150	13/16	4 × 1/2	39,100	35,300	Poor fusion 1/4 inch on side of hole
3/16 A	175	13/16	4 × 1/2	41,700	37,700	Poor fusion 1/8 inch on side of hole
3/16 A	200	13/16	4 × 1/2	44,700	40,400	Poor fusion 1/16 inch on side of hole
3/16 A	225	13/16	4 × 1/2	47,200	42,700	Complete fusion
3/16 A	250	13/16	4 × 1/2	49,000	44,300	Complete fusion
3/16 A	275	13/16	4 × 1/2	53,000	47,900	Complete fusion
3/16 A	300	13/16	4 × 1/2	56,100	50,700	Complete fusion

Table H—Plug Weld Tension Tests

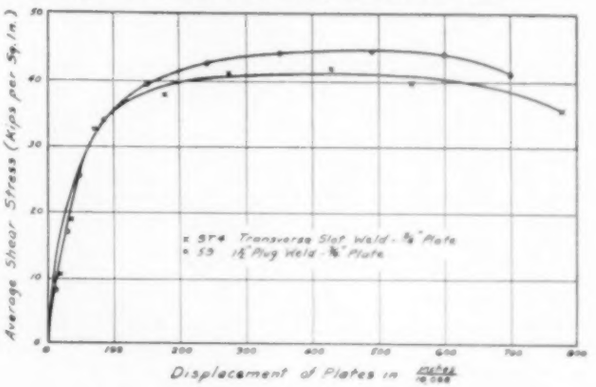
Electrode	Hole Diam. In.	Plate Thickness In.	Plug Filled In.	Ultimate Load Lb.	Shear Stress psi	Remarks
3/16 *A*	1 3/16	1/2	1/2	48,300	25,800*	Complete fusion
3/16 *A*	1 3/16	1/2	1/2	49,200	26,300*	Complete fusion
3/16 *A*	1 3/8	1	1/2	78,300	36,200*	Complete fusion
3/16 *A*	1 3/8	1	1/2	78,000	36,100*	3/16-inch blow-hole on side
5/32 *B*	1 1/4	1/2	1/2 *F*	39,400	13,700°	Good fusion, irregular fracture
5/32 *B*	1 1/4	1/2	1/2 *F*	42,800	14,700°	Good fusion, sheared through throat section

* Shear stress based on the area of the sides of the hole.
° Ultimate load per inch of fillet weld.

This probably caused some eccentricity which would reduce the strength of the plug welds made with lower currents. Increasing the heat between 200 and 225 amperes for a 3/16 electrode results in complete fusion into the corners and on the sides of the hole. All other flat plugs were welded with enough heat to give good fusion (200 to 225 amperes) so the results should be interpreted accordingly. The variation in strength of other specimens of the investigation were at least partially due to differences in welding currents. The important conclusion is that the highest practical welding current should be used in order to develop the shear strength of the weld metal and to insure complete fusion.

H. Plug Weld Tension Tests—Table H.—The plug welds for tension tests were made in a hole of a plate fusing it squarely on the end of a 1 1/2-inch round bar. This bar was pulled through a 1 3/4-inch hole in a plate bearing on the top head of the testing machine as shown in Fig. 1. In the filled plug welds a truncated cone of weld metal pulled out of the plug. The lower diameter of the cone remained on the bar and it was equal to the diameter of the plug. The sides of the cone sloped inward with an angle of about fifteen degrees from the vertical. The shear stress was computed on the basis of the area of the sides of the hole filled with weld metal. This is an approximation, but it seems to be reasonable for designing tension plugs. Based on this assumption an ultimate shear stress of only 26,000 psi was developed in the 1/2-in. plate specimen with 1 3/16-in. holes. Secondary stresses are probably present which might account for the low shearing strength. The concentration of load at the center of the plate caused it to deflect noticeably toward the center. This bending was avoided by using a plate one inch thick with the hole filled only 1/2 inch. The one-inch plate specimen fractured at a shear stress of 36,000 psi in the weld metal. The fillet plugs sheared through the throat section at about 14,000 lb. per lineal inch of fillet, the length of which was based on the circumference of its center of gravity. A 1/2-in. fillet weld will develop about 18,000 lb. per lineal inch in a fillet qualification specimen. The results of the tests in this series were only indicative and were too limited to make a definite recommendation as to the design of tension plug welds.

Fig. 9—Comparison of Slot and Plug Welds Deflection Curves


Average Shear Stress (Kips per Sq. In.)
Displacement of Plates in inches
x ST4 Transverse Slot Weld - 3/8" Plate
o 53 1" Plug Weld - 3/8" Plate

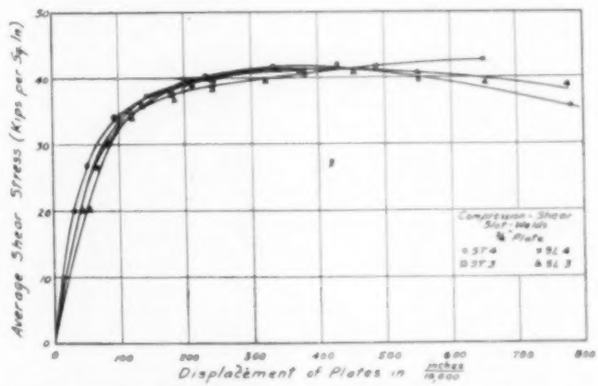


Fig. 10—Comparison of Deflection Curves of Longitudinal and Transverse Plug Welds

I. Compression Shear Tests of Slot Welds—Table I.—The purpose of this series of tests was to determine the variations due to the size and type of slot welds. The two types of slots investigated are those with the longer axis transverse to the direction of loading, and those with the longer axis longitudinal with the direction of loading. Load deflection readings were taken to show more clearly any possible difference in behavior that

Table I—Compression Shear Tests of Slot Welds

Specimen No.	Width In.	Slot Length In.	Area, Sq. In.	Test Plate In.	Plug Filled In.	Stress at Elastic Limit, psi	Ultimate Load, Lb.	Ultimate Stress, psi	Remarks
*SL*1	1 3/16	2 3/16	2.30	6 × 1/2	1/2	25,000	88,900	38,600	Complete fusion
*SL*2	1 3/16	2 3/16	2.30	6 × 1/2	1/2	25,000	89,000	38,700	Complete fusion
*ST*1	1 3/16	2 3/16	2.30	6 × 1/2	1/2	25,000	91,200	39,700	Complete fusion
*ST*2	1 3/16	2 3/16	2.30	6 × 1/2	1/2	26,000	90,800	39,500	Complete fusion
*SL*3	1 1/4	2 1/2	2.79	6 × 3/4	1/2	30,000	113,000	40,400	Complete fusion
*SL*4	1 1/4	2 1/2	2.79	6 × 3/4	1/2	30,000	118,600	42,400	Deep fracture in weld metal
*ST*3	1 1/4	2 1/2	2.79	6 × 3/4	1/2	30,000	114,000	40,800	Complete fusion
*ST*4	1 1/4	2 1/2	2.79	6 × 3/4	1/2	30,000	117,000	41,900	Complete fusion

Ends of slot rounded to diameter equal to the width.
3/16 type *A* electrode used throughout, welded in flat position.
L in specimen number means slot longitudinal to load plane.
T in specimen number means slot transverse to load plane.

Table J—Plug and Fillet Lap Joint Tests

Width In.	Plates Thickness In.	Type	Welds Plug	Welds Fillet In.	Scaling on Plug Section Lb.	Yield Point of Plate Lb.	Ultimate Load Lb.	Shear Stress Developed in Plug psi	Fracture
4	3/8	Straight	None	4 × 3/8	....	49,000	52,700	....	Plates straightened into line of pull
4	3/8	Crimped	None	4 × 3/8	....	.....	46,500	....	Crimp pulled out straight
4	3/8	Straight	1 1/8	4 × 3/8	34,000	51,000	72,800	25,000	Plug cracked on tension side and plate failed across hole section
4	3/8	Crimped	1 1/8	4 × 3/8	28,000	48,500	70,100	22,300	
4	1/2	Crimped	None	4 × 3/8	....	.....	51,000	....	Crimp pulled out straight into line
4	1/2	Straight	1 1/8	4 × 3/8	47,400	59,200	82,100	34,300	Plug failed in tension and plate failed across hole section
4	1/2	Crimped	1 1/8	4 × 3/8	52,000	59,000	78,700	30,900	Plug cracked on tension side and plate failed
6	1/2	Straight	1 1/8	6 × 3/8	76,000	117,000	122,300	50,700	Fillet and plug broke together
5	1/2	Straight	1 1/8	5 × 3/8	90,000	94,000	109,600	50,000	Fillet and plug failed together
4	1/2	Straight	1 1/8	4 × 3/8	42,600	58,000	74,300	26,500	Plug in tension and fillet together
4	1/2	Fillet on opposite side of plug	1 1/8	4 × 3/8	50,000	60,800	83,400	35,700	Plug sheared and fillet failed together
3	1/2	Straight	1 1/8	3 × 3/8	33,000	47,000	50,500	14,600	Plug failed in tension on side of hole

Shear stress on plug computed on basis of ultimate of 12,000 lb. per inch for 3/8 fillet weld in eccentric tension.
Electrode *A* used for all welds.

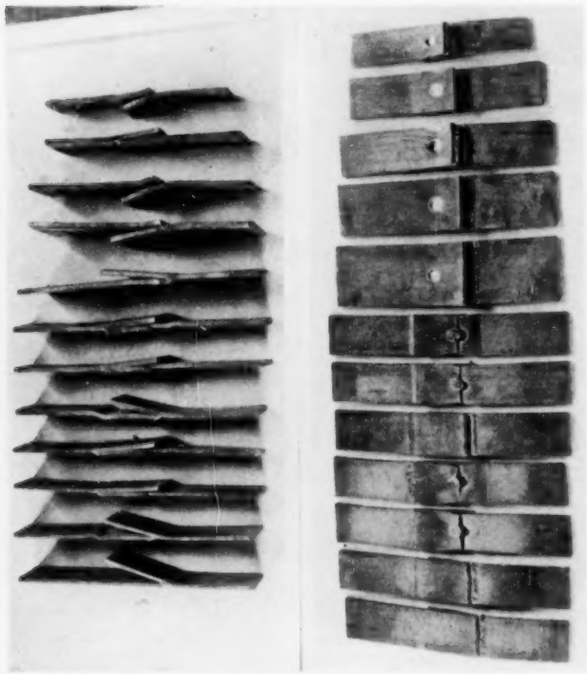


Fig. 11—Two Views of the Plug and Fillet Lap Joint Specimens Arranged in the Same Order as in Table J

could not be detected from the variations in the ultimate load. All the specimens failed in face shear with remarkable uniformity for each size of slot. The smaller slots in the 1/2-inch plate failed at lower shear strengths than the values obtained on the larger slots in 3/4-inch plate. The thinner plates were not stiff enough to evenly distribute the load over the entire weld and resulted in the progressive failure as described in Section *B*.

The strength and the deflection curves of the larger slots compare well with plug welds (Fig. 9). The sizes of slots studied show no distinct difference between the transverse and longitudinal tests as shown in Fig. 10. These curves show an elastic limit of about 30,000 psi. for both slots and plug welds. The shape and length of the curves above the elastic limit show that welds of this type are remarkably tough and will not fail suddenly.

J. Plug and Fillet Lap Joint Tests—Table J.—The purpose of this series was to determine effectiveness of plug welds in combination with fillet welds where the various spacing of the plugs is taken as the width of the test specimen. This is the ideal case of isolating a section of wider joint in which one plug weld is included, the width of the section being the spacing of the plug welds. A comparison of the distortion of plates caused by plug and fillet welds was also studied. Figure 11 shows two views of the specimens arranged in the same order as Table *J*. Fillet welds were tested without plug welds to determine the approximate strength added by the plugs.

During testing, all the specimens bent at the joint so that the ends of the specimen were along the line of pull. When the fillets were tested without plugs the free end of the plate at the joint bent twenty degrees away from the other plate. This causes a high concentration of stress at the root of the fillet, and limits the strength of the 3/8-in. fillet to about 12,000 lb. per lineal inch. Crimping the plates into line benefits this condition very little because the weld is still eccentric. It is believed that if a plug weld was added to keep the plates from separating there would be a twofold addition of strength; the strength of the plug itself and the strength added to the fillet by avoiding the stress concentration at its root. However, in the lap joint tests the failure took place in such a manner that not even the full shear strength of the plug was developed in all cases. The primary failure occurred at the plug weld on the tension side of the hole (the side opposite the fillet weld). The cause of this primary failure was due to the yielding of the plate across the section at the plug which resulted in a stress concentration and a failure on the tension side of the plug. This failure transfers part of the load carried by the plug to the fillet which finally fractures. The plug then receives the load in bearing on the compression side of the hole and consequently fractures in shear. If the test plate was not wide and thick enough, it fractured before the fillet weld across the net hole section. This fracture of the plate occurred because of the stress concentration of the reduced section (Fig. 12).

Plug welds develop their ultimate shear stress in proportion to the amount of stress concentration which depends on the width of the plate or the spacing of the plugs; there being only one plug weld in each specimen. The five- and six-inch wide plates showed a relatively higher ultimate shearing stress than a simple plug weld because of the inaccuracy in assuming an ultimate strength of 12,000 lb. per lineal inch for a 3/8-inch fillet.

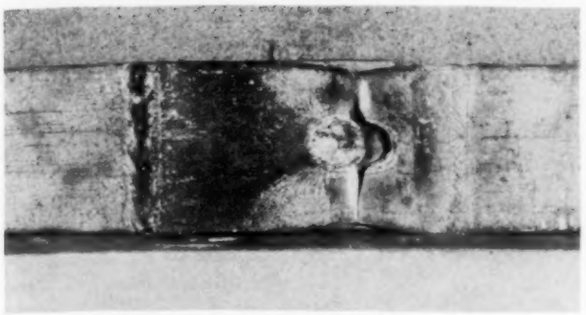


Fig. 12—A Plug and Fillet Lap Joint Specimen Showing Tearing of Plate Across Plug Section

This series of tests indicate that the spacing of plug welds on a line should not be less than six diameters. Other tests not listed in the table indicate that when the plugs are made on the opposite side of the joint from the fillet, the load carried by the fillet does not have to be carried across the net section and the spacing may be somewhat less. These tests also indicated that the spacing of the plug from the fillet should be about four diameters.

The measurements of plate distortion due to a combination of plug and fillet welding were made on a series of 3 by $^3/_8$-inch lap jointed plates, using $1^1/_{16}$-in. plug and $^3/_8$-inch fillet welds. The plugs were welded first and there was no noticeable or measurable bending out of line. The plug weld held the plates together with a clamping action. The fillets were made in the flat position using two passes of $^3/_{16}$-inch electrodes. Figure 13 shows a straight edge resting on the top of a plug welded specimen and there is no noticeable bending out of line. The specimen underneath was also fillet welded and a noticeable bend in the plate can be seen at the fillet weld. There is little difference in the bending of the plates due to the fillet weld if the plates are clamped together or if they are plug welded. The deflection of the free end seven inches from the weld measured from the original line of the plates was approximately $^1/_4$ inch or two degrees. This difference in distortion of the two types of welding is worthy of consideration. The general indications of this series of tests are that plug welds should be spaced transversely at least six diameters when acting with fillet welds and about four diameters from the fillet in order to develop the strength of both welds. The distortion of plates due to plug welds is much less than that due to fillet welds.

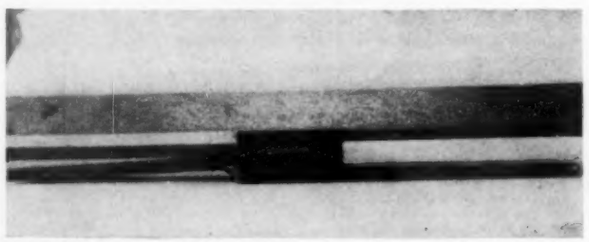


Fig. 13—Plate Distortion Plug Weld Joint (Top), Fillet Welded Joint (Bottom)

Summary and Conclusions

1. Complete face fusion is possible; therefore the strength of plug welds should be based on the shear of the gross area of the hole.
2. The filled type of plug weld has many advantages over the fillet types; they are quicker and easier to make, their strength is more uniform, they do not require as large a hole, and they have a much better finished appearance.
3. There is no danger of the filled plug cracking due to the shrinkage of the weld metal when made with modern heavy coated electrodes.
4. Large plug and slot welds fracture at reduced shear values because of a tendency toward progressive failure.
5. There is a minimum size of hole for each thickness of plate that can be successfully welded.
6. There is a maximum size of hole for each thickness of plate capable of developing the full shear strength of the plug weld.
7. The face shear failure of plug welds occurs in the "burnt steel" surface of the fusion zone which results in the low shear strength of plug welds.
8. The shear strength of the weld metal from various brands and types of electrodes tested varied between 47,400 and 53,800 psi.
9. Plug welds can be successfully welded in a vertical position.
10. The highest practical welding current for the electrodes used should be employed for plug welds in order to develop the shear strength of the weld metal and to insure complete fusion.
11. Plug welds are remarkably tough and will not fail suddenly.

Table K—Recommended Size and Design Loads for Plug Welds

Plate Thickness In.	Hole Diameters In.	Depth of Plug In.	Design Load° Lb.
$^1/_4$	$^3/_4$	$^1/_4$*	5,000
$^5/_{16}$	$^7/_8$	$^5/_{16}$*	6,800
$^3/_8$	1	$^3/_8$*	8,900
$^7/_{16}$	$1^1/_{16}$	$^7/_{16}$*	10,000
$^1/_2$	$1^1/_8$	$^7/_{16}$	11,200
$^9/_{16}$	$1^3/_{16}$	$^1/_2$	12,500
$^5/_8$	$1^1/_4$	$^1/_2$	13,800
$^3/_4$	$1^3/_8$	$^9/_{16}$	16,800
1	$1^1/_2$	$^9/_{16}$	20,000

* Should be capped $^1/_{16}$ inch extending $^1/_8$ inch around outside edge of hole.

° Based on AMERICAN WELDING SOCIETY design shear stress of 11,300 psi over the area of the hole.

12. The distortion of plates due to plug welds is much less than that due to fillet welds.
13. The recommended sizes and design loads for plug welds are recorded in Table *K*.
14. When plug welds are used together with fillets, the combined strength of both will only be obtained when a certain minimum spacing of the plug welds is used.

Peening and Its Effects on Arc Welds

By E. M. MacCUTCHEON, Jr., and D. M. KINGSLEY, Jr.

Synopsis

I. Purpose and Scope

The purpose of this investigation is to determine whether various amounts of hot or cold peening are benificial in reducing shrinkage strains due to electric arc welding, and whether this peening is injurious to the physical properties of the metal.

II. Results

	Hot Peening		Cold Peening	
	Others	Ours	Others	Ours
Strain Relief	Better	Better	Best	Best
Tensile Strength	Better	Better	Best	Best
Ductility	Good	Good	Good	Good
Bend Ductility	Good	Good	Good	Good
Hardness	Better	Better	Best	Best
Fatigue	Better	...	Better	..
Impact	Better	...	Better	..
Grain Structure	Good	Good	Good	Good

(Standard: Non-peened = Good)

III. Conclusions

Hot peening does not reduce shrinkage distortions in constrained work, cold peening is effective, however. Cold peening reduces the strength of single bead fillet welds. Cold peening is bad for the bend ductility of welds.

Peening of weld metal, even to somewhat beyond flaking of the surface, increases the tensile strength and ductility. Peening butt welds tends to separate the butting plates.

Scope.—Unconstrained V-butt and fillet weld specimens were studied for tensile strength, ductility, bend ductility, hardness and grain structure changes under peening.

Apparatus and Methods.—The fillet weld specimens were prepared from $^3/_8$-inch plate[1*] welded on to 1-inch plate, as shown in Fig. 1. A single bead[2*] was run down both sides of the joint. "Hot" peening of the fillet weld specimens was done immediately after welding, and the "cold" peened specimens were allowed to cool so as to be only hand warm before peening. The specimens were then burned apart and machined to finished dimensions as shown in Fig. 1. No yield point or ductility information was collected on these specimens. By careful measurement with a scale after failure, the section areas in shear and tension were determined, and thus reasonably accurate figures were obtained for unit stresses.

The V-butt specimens were prepared as shown in Fig. 2. The plates were first dogged down on the bending slab with the backing strips in place. Then three tacks were placed, one at each end and one in the middle of the butt. The clips for strain measurements having been welded on, micrometer readings before welding were obtained. The method of obtaining strain readings is described under the section headed "Strain."†

"Hot" peening of V-butt specimens means that each bead but the last was peened immediately after welding,[3] the last bead acting as an annealing bead. The surface of the "cold" peened specimens were peened after the metal had cooled from the welding operation to hand warm. After welding and peening, the butt-weld specimens were cut apart and machined to finished dimensions. The separate coupons were then tested to destruction. Ductility measurements were obtained by using dividers between center punch marks gaged 2 inches apart. When the balance bar dropped and failed to rise, the final reading was obtained. This was not quite accurate, but the strain gage could not be used up to the limit desired.

The bend test specimens were welded in the same manner as the V-butt specimens just described, and this is as required by the American Bureau of Shipping. Machining was done as is indicated in Fig. 3. The bend speci-

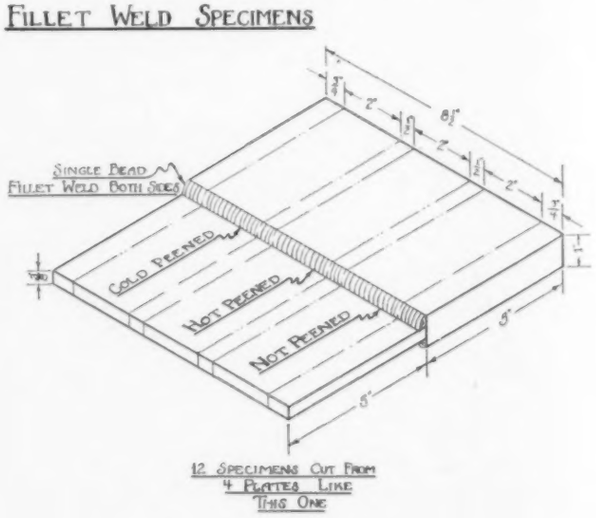


Fig. 1

Extract from Senior Thesis—Webb Institute of Naval Architecture. Contribution to Fundamental Research Division, Engineering Foundation Welding Research Committee.

* Exponents 1–4 refer to appendix.

† No distinction is made in this report between strains and distortions.

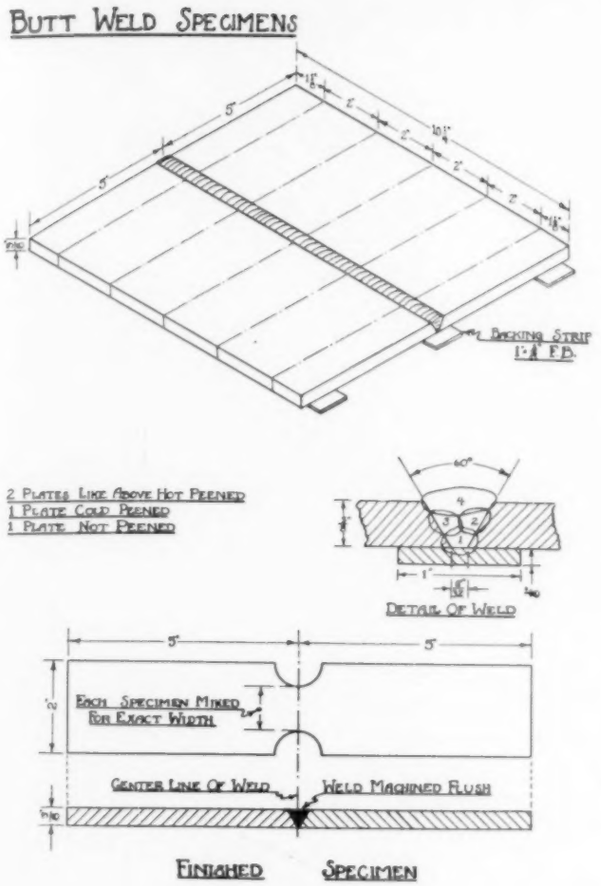


Fig. 2

mens were given their initial bend of 30° on each end by placing them in a vise and striking the end with a maul. Scribe marks were made $^1/_{32}$ inch inside the edge of the weld, and the initial distance between these marks was determined. Then, each specimen was placed successively in the testing machine as a strut, and pressure was applied gradually at the ends until failure occurred in the outside fibers. This test was performed in accordance with the American Bureau of Shipping rules.

Photomicrographs were made of grain structure of a great many specimens, and a complete discussion of this is given in the section headed "Grain Structure."

Hardness tests were made on a specially welded pad, showing the effect of peening the surface and also the effect of the annealing bead on the hardness.

Strain (or Distortions)

Strain readings were taken on all butt welds, hot, cold and non-peened specimens with the exception of those specimens intended for the free-bend test. No attempt was made to measure the strain across the fillet welds, since the only direction in which the measurement could have been made, was not the important one in this case.

The method used to obtain the strain readings was it is believed, unique. Instead of measuring between punch marks on the surface of the plate with a strain gage, lengths across the weld were found by using an inside micrometer between clips on either side of the weld. These clips, of $^1/_8$-inch steel flat bar and about 1-inch square, were welded on the middle line of each specimen as indicated by the scribed marks on the laid out plate. These clips should be securely welded to the plate, not just tacked, as peening caused several of them to jar loose and come off.

The distances between the clips were found before welding for all the specimens, after welding for the cold and not peened specimens, and after peening for the cold and hot peened specimens. Since every bead but the last one was peened on the hot peened specimens, no after-welding readings could be obtained. In all cases, the plates were allowed to cool before any measurements were taken. Due to the single V-butt joint used, more shrinkage occurred across the top of the weld than across the bottom. This caused the plate to assume an angle, and it became necessary to apply a correction factor to the micrometer readings to get a true result.

In the Appendix, an analysis is given of the stresses imposed on a plate when joined by a single V-butt weld of 4 beads, similar to that used in preparing these specimens. This analysis seems to show that the location of the neutral axis after welding is still very near the center of the thickness of the plate. With this fact in mind, the correction factor was deduced as is shown in the Appendix. When the micrometer reading "a," is the angle that the plate has warped "θ," are substituted in the formula, the result "x," gives the true length along the neutral axis between the clips. The results of all the strain readings and calculations are given in Table 1.

Specimens numbers one through four, cold, hot and not peened, were prepared from plates as shown in Figs. 4 and 5. It is interesting to notice that the after welding strains for cold and not peened Nos. 1 to 4 are in very good agreement and can be considered as representative in unrestrained work of this sort. Cold peened speci-

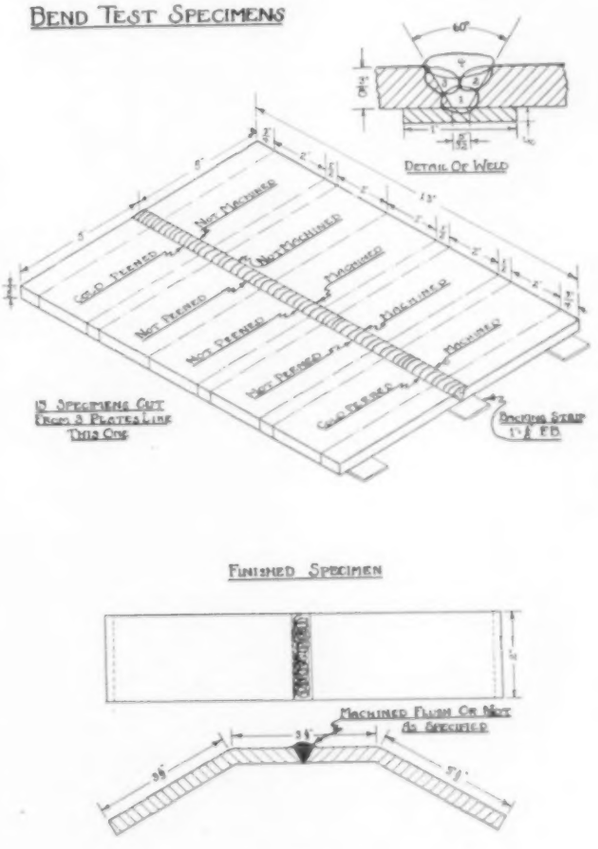
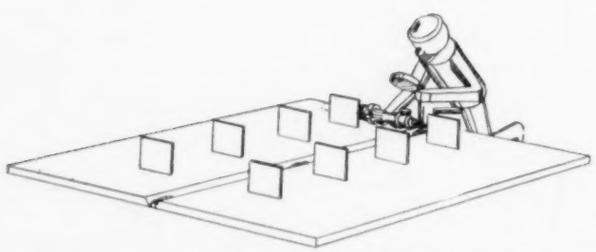


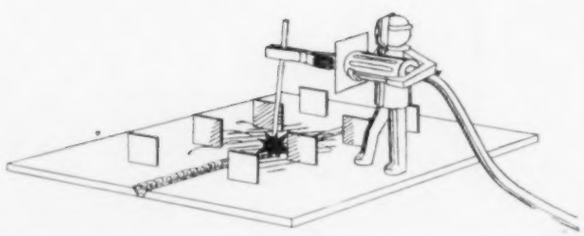
Fig. 3

Table 1—Distortions Due to Welding and Peening—All Readings in Inches

Specimen Type	No.	Micrometer Before Welding	Micrometer After Welding	Angle θ_1	Micrometer After Peening	Angle θ_2	Corrected Readings After Welding	Corrected Readings After Peening	Strain Due To Welding	Strain After Peening	Reduction Of Strain	Percentage Reduction Of Strain	Remarks
Unpeened	1	2.7941	2.7114	3°13′	———	—	2.7511	———	0.0430	———	———	———	Specimens Prepared At
	2	2.8418	2.7582	"	———	—	2.7979	———	0.0438	———	———	———	Federal S.B.&D.D.Co; All
	3	2.8060	2.7250	"	———	—	2.7647	———	0.0413	———	———	———	In One Plate–Gives A
	4	2.8737	2.8010	"	———	—	2.8407	———	0.0330	———	———	———	Good Idea Of Average
	Av.								0.0403				Strains Due To Welding.
Cold Peened Moderate	1	2.7517	2.6602	3°41′	2.7177	0	2.7057	2.7177	0.0460	0.0430	0.0120	26.1%	At Federal: All Specimens
	2	2.7500	2.6610	"	2.7170	"	2.7065	2.7170	0.0435	0.0330	0.0105	24.2%	In One Plate; Large
	3	2.7778	2.6874	"	Clip Off	"	2.7329		0.0449				Tool And Too Light
	4	2.7672	2.6717	"	Clip Off	"	2.7172		0.0500				Peening Gave Low
	Av.								0.0466				Strain Reductions
Cold Peened - Hard	5	Clip Off	2.4331	1°42	2.4829	0	2.4535	2.4829			0.0294		At Consolidated: 4
	6	2.3870	2.3356	2°37′	2.3952	"	2.3677	2.3952	0.0193	0.0082	0.0275	142.4%	Single Coupons; Small
	7	2.5189	2.4569	3°25′	2.5258	"	2.4990	2.5258	0.0199	0.0069	0.0268	134.6%	Tool Used And Evident-
	8	2.5275	2.4528	4°37′	2.5355	"	2.5144	2.5355	0.0131	0.0080	0.0211	161.0%	ly Peened Too Hard
	9	2.7415	2.6814	3°23′	2.7347	"	2.7231	2.7347	0.0184	0.0068	0.0116	63.0%	At Federal: 4 Single
	10	2.8317	2.7604	2°58′	2.8125	"	2.7970		0.0347	0.0192	0.0155	44.7%	Coupons; Not Peen-
	11	2.7772	2.7188	3°23′	Clip Off	"	2.7605		0.0167				ed Hard Enough To
	12	2.8235	2.7794	2°35′	Clip Off	"	2.8112		0.0123				Relieve All Strain
	Av.												Due To Welding
Hot Peened	1	2.8069	———	—	2.7619	1°48½′	———	2.7839	———	0.0230	———	———	At Federal: All In One
	2	2.7889	———	—	2.7384	2°2′	———	2.7633	———	0.0256	———	———	Plate; Average Strain
	3	2.8342	———	—	2.7782	2°15′	———	2.8055	———	0.0287	———	———	After Hot Peening
	4	2.7888	———	—	Clip Off		———		———		———	———	Subtracted From
	Av.								0.04343	0.02573	0.01772	40.8%	Average After Weld-
	9	2.9126			2.9440	1°11½′		2.9885	0.0166	0.0459	0.0625	376.0%	ing In Unpeened &
	10												Cold Peened #1-4



Strain Measurement with Inside Micrometer Before Welding Tacked at Each End and Middle



Welding—Beads 1 and 3 One Way, Beads 2 and 4 the Opposite Way

Fig. 4

mens Nos. 6 to 12 were prepared as single coupons, 2 inches wide, and the after welding strains on these, although in good agreement with each other, average less than half the strain as shown by Nos. 1 to 4.

This difference may be explained by noting the difference in the mass of the metal involved in the two types of specimens. Considering the four specimens all in one plate (see Fig. 2), each bead cools considerably due to radiation and conductivity of the surrounding metal, before the application of the successive bead. Hence, relatively rapid cooling of *each bead* from high welding temperatures causes contraction in the case of *each bead*, and the resultant shrinkage is rather high. With the smaller 2-inch coupons, the mass is much less, and the heat is unable to pass off fast enough, the result being that the whole specimen becomes quite thoroughly warmed up. Therefore, because the heat is more distributed, there is less shrinkage concentrated across the weld than in the case of the four specimens in one plate.

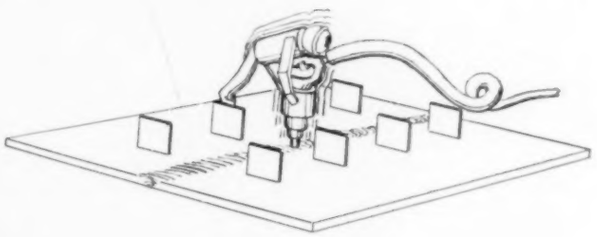
Of the first four cold peened specimens, but two final percentages of strain reduction were obtained because on two of the specimens the clips jarred off. These percentages are rather low because first, the weld was moderately peened, and second, a large tool was used resulting in a relatively low peening pressure. Cold peened specimens Nos. 5 to 8 were not prepared by the authors, but by a welder at the Consolidated Yacht and Engine Building yard. These specimens were peened heavily with a small tool, and the resulting percentage change of strain shows the efficacy of this procedure.

Table 2

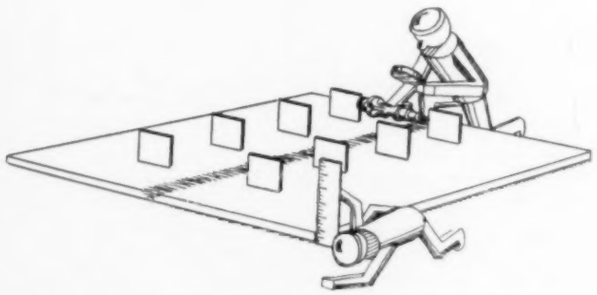
RESULTS FROM FILLET WELD TENSION TESTS

TYPE OF SPECIMEN	MAXIMUM LOAD IN LBS.	APPROXIMATE AREAS MEASURED WITH SCALE AFTER RUPTURE. IN IN.²			APPROXIMATE UNIT STRESS IN LBS./IN.²
		SHEAR	TENSION	EQUIVALENT TENSION *	
UNPEENED #1	37,460		0.5320	0.5320	70,400
#2	40,040	0.5410	0.1800	0.5595	71,650
#3	39,360		0.5155	0.5155	76,300
#4	40,060	0.4160	0.2535	0.5442	73,650
AVERAGE	39,200	0.2395	0.3710	0.5385	73,000
COLD PEENED #1	35,130		0.4670	0.4670	75,500
#2	27,900	0.0350	0.4110	0.4355	64,200
#3	39,130	0.1000	0.5085	0.5785	68,600
#4	47,230		0.6470	0.6470	73,000
AVERAGE	37,300	0.0338	0.5080	0.5316	69,800
HOT PEENED #1	34,350		0.5370	0.5370	64,000
#2	25,170		0.3910	0.3910	64,200
#3	21,900		0.3910	0.3910	56,000
#4	25,010		0.4400	0.4400	56,800
AVERAGE	26,600	0.0	0.4390	0.4390	60,500

* Assuming that 1 in.² in shear area is equivalent to 0.7 in.² in Tension.



Peening



Stress Measurement with Micrometer and Rule After Welding and After Peening

Fig. 5

The two final results from cold peened specimens Nos. 9 to 12 gave indication of good stress relieving, although they were not peened hard enough to relieve all the strain due to welding.

The "hot" peened specimens were peened immediately after welding on every bead but the last. The first four hot peened specimens were prepared all in one plate. The average strain after welding, of the non-peened and cold peened specimens, which were also prepared four in one plate, was used as a figure from which to subtract the average strain after peening of the above mentioned hot peened specimens. This seems to be a logical procedure, especially since these specimens were all prepared at the same time and in the same manner. The strain reduction here, 40.8%, is good, but it might have been better if a smaller tool and harder peening had been used.

Table 3

RESULTS FROM TENSION TESTS ON BUTT WELD SPECIMENS

TYPE OF SPECIMEN	ELONGATION IN 2 INCHES	MAXIMUM LOAD IN LBS.	AREA IN IN.²	ULTIMATE STRENGTH IN LBS./IN.²
UNPEENED #1		25,520	0.3280	77,800
#2	.17	24,560	0.3281	74,800
#3	.125	22,940	0.3082	74,400
#4	.125	23,385	0.3216	72,600
AVERAGE	.105			74,900
COLD PEENED #6	.03	19,555	0.2492	71,100
#7	.02	20,850	0.2815	73,500
#8	.02	18,070	0.2348	76,900
#9	.08	21,315	0.2899	73,500
ONLY THESE 4 ARE AVERAGED #10	.15	28,540	0.3198	89,400
#11	.12	24,810	0.3038	81,200
#12	.11	25,975	0.3372	77,000
AVERAGE	.115			80,300
HOT PEENED #1	.135	26,160	0.3653	73,600
#2	.10	21,985	0.2947	74,500
#3	.10	22,875	0.3138	73,000
#4	.09	24,046	0.3357	71,800
#6	.06	19,235	0.2627	73,200
#7	.10	21,005	0.2886	72,800
#8	.10	21,030	0.3004	70,000
#9	.13	26,530	0.3840	69,000
AVERAGE	.116			72,300

Hot peened specimens Nos. 9 and 10 were prepared after the first ones, and from 2-inch wide coupons. These two were peened very vigorously for the express purpose of finding out what effect this would have. An average of strains for after welding of 2-inch coupons was obtained from cold peened specimens Nos. 6 to 12, and from this was subtracted the strain after peening of hot peened No. 9. After peening strains were considered negative when after peening readings were greater than those before welding.

The resulting strain "reduction" for hot peened No. 9, 376%, indicates that the specimen must have elongated considerably under the severe peening. However, this specimen gave a good tensile test as is discussed else-

where in this report (Table 3). It can be deduced from this that quite a large change in strain can be accomplished when hot peening without injuring the physical properties of the metal. Specimen No. 10, hot peened, failed while being peened, thus indicating a limit which should be avoided.

In all the work done by other experimenters, information concerning shrinkage strains is the least definite. Jennings states that peening is a very effective means of reducing shrinkage strains, but he gives no quantitative information of the results of his tests. Westfall gives 0.05 inch average shrinkage in 2 inches for unpeened specimens. This is in good agreement with what has been found in this report for similar unconstrained specimens.

Tensile Strength

In general peening apparently helps the tensile properties of welds.

The tension tests for this report were conducted on two different types of specimens. The first was a fillet weld specimen as shown in Fig. 1 and Table 2. The results show that peening is detrimental to a single bead weld in tension. Tests by the navy and other experimenters substantiate this and it is not considered good in the present day shipyard practice to peen single bead welds. It is sometimes done however when it is more important to relieve strain than to have unimpaired tensile strength.

The fractures in the hot peened fillet weld specimens showed well defined injuries due to the peening. This is also reflected in the approximate areas as given in Table 2. The difference in the amount of area in shear would seem to indicate that peening increases the strength in shear but this is not definite.

The other specimens were of the type shown in Fig. 2 and Table 3. The unusually high values may be due partly to the small size of the specimen but Harrelson's tensile specimens were only $^3/_4$ inch in diam.

The results of cold peened specimens Nos. 1 to 5 were discarded because a flaw ran all the way through them and the results of Nos. 6 to 8 were not averaged because they were done by a commercial operator and had many flaws so we had to reduce the area considerably in the machining.

Ductility

The figures for ductility found in this investigation are given in Table 3. These figures may seem relatively low, but they are not bad when the type of specimen used is considered. It is interesting to notice that the ductility has been slightly increased for both hot and cold peening. This is in substantial agreement with what is noted above on other experimental work in peening.

There is rather conclusive evidence that neither hot or cold peening have any deleterious effects upon the tensile ductility of welded joints.

Bend Ductility

Table 4 gives the figures on the bend tests. There is little difference between the per cent elongations of the non-peened and the hot peened machined specimens. The cold peened machined specimens were considerably poorer than the non-peened machined. Also the unmachined specimens cold peened were much poorer than the unmachined unpeened ones. The machined specimens had the reinforcement cut off and the surface of the weld flushed up where possible, and the sharp right angle corners were rounded off with a file. It is worth comment that the machined specimens, both non- and cold peened, gave better elongations than the specimens which were tested just as welded.

The hot and non-peened specimens showed excellent plasticity when bending. Failure, when it occurred, started at the edges or pin point flaws which spread slowly across the plate. On the other hand, the cold peened specimens snapped completely across the plate when the bending had just started.

Joints liable to bending stresses should not be cold peened to reduce the shrinkage strains. Hot peening is all right in this connection, but it must be kept in mind that, in practice the joint will be weaker in bend ductility than the *specimen shows when machined.*

Hardness

In these tests a special specimen was used. Two layers of beads were laid on a $^3/_8$-inch plate and peened. Over half this surface annealing beads were made.

The hardness tests were made at various depths throughout the thickness of the plate below the peened half of the surface and again below the surface with the annealing bead. Forty-seven tests were made, five gave a hardness of the base metal of 66.9 Rockwell "B."

Table 4

Results from Free Bend Test Specimens

Type of Specimen		Initial Distance in $\frac{In.}{64}$	Final Distance in $\frac{In.}{64}$	Difference in $\frac{In.}{64}$	Elongation in Percent
Not Peened Machined	#1	48	Improperly machined		
	#2	47	65	18	
	#3	50	63	13	
	Average	49		16	32.8
Hot Peened Machined	#1	44	59	15	
	#2	48	63	15	
	#3	50	65	15	
	Average	47		15	32.8
Cold Peened Machined	#1	48	52	4	
	#2	49	53	4	
	#3	48	56	9	
	Average	48		6	12.5
Not Peened Unmachined	#1	46	Flaw		
	#2	57	62	5	
	#3	42	51	4	
	Average	52		4.5	8.8
Cold Peened Unmachined	#1	44	46	1	
	#2	48	50	1	
	#3	53	54	1	
	Average	48		1	2.1

Remarks: Cold Peened Specimens Cracked Suddenly Right Across With a Sharp Ringing Snap. (Like Glass)

Three gave a hardness on the peened surface of 102 Rockwell "B." The other tests are plotted in Fig. 6. They were taken at two different cross sections to give average results. The surface of the annealing bead would be about 0.62 inch or slightly above the border of the figure.

These tests indicate that peening affects the hardness to a depth of about 0.5 inch and the annealing bead is effective to a depth of 0.3 inch below the peened surface.

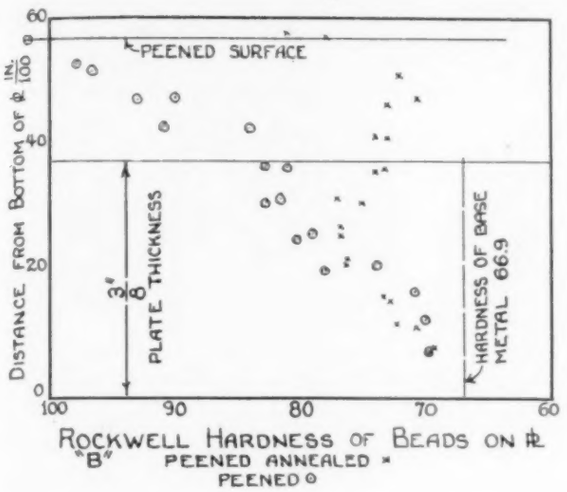

PEENED SURFACE
DISTANCE FROM BOTTOM OF PLATE IN 1/100
3/8" PLATE THICKNESS
HARDNESS OF BASE METAL 66.9
ROCKWELL HARDNESS OF BEADS ON PL "B"
PEENED ANNEALED ×
PEENED ⊙

Fig. 6

The hardness of our hot peened welds would be about 77 Rockwell "B" and about 73 Rockwell "B" on the unpeened or about 6% increase in hardness due to multiple bead peening.

Conclusions

Cold peening is evidently the more effective method of reducing the shrinkage strains in welds. The bend ductility of cold peened welds is poor. On the other hand hot peening which gives shrinkage strain reductions almost as great and almost as easy to control as cold peening has very good physical properties.

Peening is injurious in single bead welds.

In rigidly constrained welds hot peening has little or no effect on the reduction of shrinkage strains. Cold peening, however, is effective.

Hot peening of each bead represents an excellent method of preventing welding cracks from forming in multiple bead welds.

The following excerpt from "X-Ray and Welding" by UNDERWOOD and ASH is recommended as being representative of good peening practice:

45. Peening is the most potent means of controlling stresses, preventing cracks and avoiding distortion. This operation must be very carefully done if it is to be effective, however, for misuse will be equally harmful. When peening is to be employed, layers of welded metal should be deposited so that their surfaces are as smooth as possible.

46. Irregularities that occur should be removed by chipping or grinding so equal work will result throughout the deposits from application of the peening tool. All slag or foreign material must, of course, be carefully removed. Peening should follow the smoothing and cleaning operation as quickly as possible.

47. The peening operation is preferably accomplished by means of a rounded or blunt nosed tool in an air hammer. Rapid light blows should be used so that the metal will flow smoothly, as in the heading of a hot rivet, rather than by heavy blows which may tend to tear or rupture the metal. By peening immediately after welding, while the deposit is still hot, less work is required to accomplish a given result and the metal is more ductile so that detrimental effects are less likely to occur. Peening at this time also prevents contractive stresses which would possibly cause distortion or cracking.

Peening Precaution

48. If the material being welded is subject to air hardening, care must be taken not to peen the first two or three beads or layers, as blows from the peening hammer will be inducive to cracking in the hardened zone beneath the weld metal deposit. Where the cavity to be repaired extends all the way through the casting the first two layers of weld metal closing the void should not be peened. Sections of this nature may be too thin

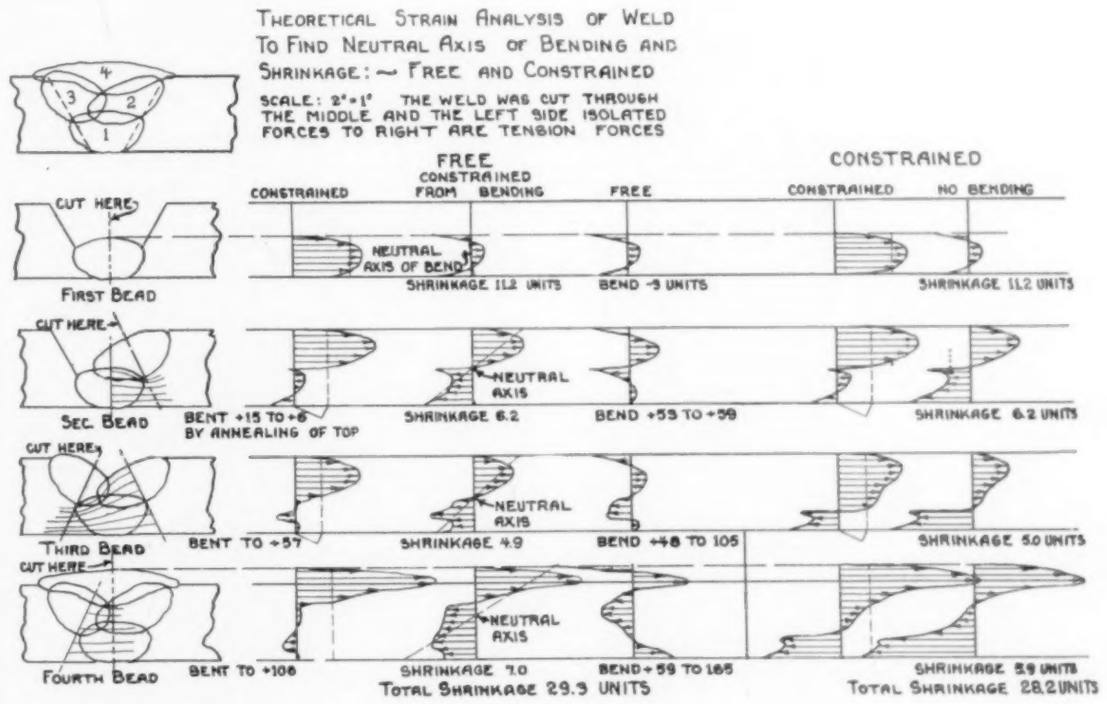
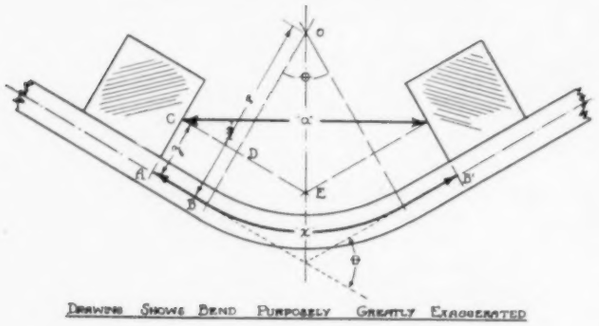


Fig. 7

STRAIN READING CORRECTION



DRAWING SHOWS BEND PURPOSELY GREATLY EXAGGERATED

LET $BB' = \omega$ AND $OB = r$; THEN:- $\omega = r\theta$

NOW WE HAVE:- $x = 2AB + \omega$

AND:- $AB = \frac{x-\omega}{2}$

NOW LET $AC = y$, THEN $DE = (r-y)\tan\frac{\theta}{2}$, AND $CE = \frac{a}{2}\cdot\frac{1}{\cos\frac{\theta}{2}}$

WE SEE THAT:- $AB = CD - CE - DE = \frac{a}{2}\cdot\frac{1}{\cos\frac{\theta}{2}} - (r-y)\tan\frac{\theta}{2}$

OR $\frac{x-\omega}{2} = \frac{a}{2}\cdot\frac{1}{\cos\frac{\theta}{2}} - (r-y)\tan\frac{\theta}{2}$

SOLVING FOR x:- $x = \frac{a}{\cos\frac{\theta}{2}} - 2r\tan\frac{\theta}{2} + 2y\tan\frac{\theta}{2} + \omega$

$x = \frac{a}{\cos\frac{\theta}{2}} + 2y\tan\frac{\theta}{2} + \omega\left(1 - \frac{\tan\frac{\theta}{2}}{\frac{\theta}{2}}\right)$

FOR SMALL ANGLES $\frac{\theta}{2} = \tan\frac{\theta}{2}$, HENCE OUR CORRECTION IS:-

$$x = \frac{a}{\cos\frac{\theta}{2}} + 2y\tan\frac{\theta}{2}$$

Fig 8

to withstand the mechanical working and subsequent cracks may possibly develop.

49. Care must be taken that no undue irregularities in the weld surface are peened down, as this will cause folds in the underlying metal which will be covered over and remain as a void in the deposit. Excessive peening must be avoided, as compressive forces can be introduced which are detrimental and in extreme cases may cause warping in the reverse direction from the anticipated. Personnel can be trained so that they can perform peening operations efficiently. Little supervision is required once a suitable routine has been established. If peening is properly done it is very helpful in preventing distortion and in the production of satisfactory weld repairing.

Appendix

1. Steel.—The plate used in preparing the specimens was ordinary mild steel of about 0.20% C.

2. Electrodes.—The welding rod used was a heavily coated $^5/_{32}$-inch electrode. The carbon content of this electrode is between 0.14% and 0.16%.

3. Peening.—The hammer used was the "Super" Chipping Hammer No. 2 manufactured by Will H. Keller Inc., and a constant air pressure of 90 #/sq. in. was maintained. The end of the tool used for peening the fillet specimens, the bend specimens, and the butt specimens Nos. 1 to 4 was about 0.20 sq. in. in area. All the other peening was done with a tool of about 0.1 sq. in. area.

4. Theoretical Strain Analysis of the Weld.—This analysis was made in order to approximate the position of the neutral axis of the bend when the plate was welded free and the comparative amount of shrinkage between free plates and plates constrained from bending but free to move perpendicular to the weld.

The following assumptions were made;

1. The shrinkage at any point is proportional to the width of the bead at that point.

2. The stresses traveled almost intact from one plane of consideration to another. The locked-up stresses not being absorbed by the sheering elasticity as the distance from the weld increased.

In the analysis the plate was assumed to shrink without bending and then to bend until in equilibrium.

Each bead was assumed to have annealed some of the locked-up stresses in the previous bead. In light of the hardness tests Fig. 6 it would seem that each bead annealed to a depth greater than was assumed in the analysis. However the difference is negligible in the result as the whole analysis is merely an artifice.

Comments on Literature, Continued from page 3)

d'Essais des Materiaux, Université libre de Bruxelles, Belgium

"Thank you very much for the advance copy of the digest of literature on 'Fatigue Tests of Welded Joints,' which you sent with your letter of November 3rd.

"I must admit that I have looked through it somewhat hurriedly, but closely enough to appreciate that it is a very excellent piece of work. At the moment, I do not have any comments or suggestions for revision to make. However, I am circulating it among the members of our organization who are most interested. Perhaps they may have some comments to offer. If so, I shall be more than pleased to pass them on to you."

O. B. J. FRASER, Superintendent of Technical Service Mill Products, The International Nickel Company, Inc. New York.

"With reference to your letter of November 4th, with which you sent a copy of the review of the literature on "Fatigue Tests of Welded Joints," I have had two members of my technical staff review this report in addition to going over it in detail myself. We are all very much impressed with the amount of work which has been done in this field as well as the way in which you and your associates have summarized the existing information. We have no specific criticism to offer, but wish to express our heartiest commendation for the way in which the work has been carried out."

R. L. TEMPLIN, Chief Engineer of Tests, Aluminum Research Laboratories, Aluminum Company of America, New Kensington, Pa.

"I received the report you sent me concerning Fatigue Tests of Welded Joints, which I will read with great interest.

"I congratulate you on the importance of the work."

A. PORTEVIN, 32 Boulevard de la Chapelle, Paris, France

"We have carefully read your review on 'Fatigue Tests of Welded Joints' and are convinced that it is a very worth while piece of work.

"We compliment you on having clearly separated the variables in fatigue testing welds and giving numerical data. . . . We consider that the chapter on Methods of Design as used in different countries is very explanatory, as are also the chapters on Service Results, Boilers, Bridges, Tubes and Typical Joints. Particularly important is the very complete Bibliography which omits practically nothing. All in all, we believe that the present Review will be very worth while for practice."

VEREIN DEUTSCHER INGENIEURE—Fachausschuss für Schweisstechnik (Welding Committee)

"We admire the great and extraordinarily efficient work contained in this review and deplore the difficulties of translation which hinder the majority of German engineers."

DR. ADRIAN, *V. D. I.*

"In Number 1, 1937 of the JOURNAL of the AMERICAN WELDING SOCIETY we have found an article by you and Dr. G. E. Claussen on 'Fatigue Strength of Welded Joints—A Review of the Literature to October 1, 1936.' We find this review extraordinarily worth while and will mention it in our magazine *V. D. I. Zeitschrift*. May we have another copy of the Review. May we also have copies of any other reviews as they appear?"

PAREY, Editorial Staff, *V. D. I. Zeitschrift*

Welding Cast Steel

It would be desirable for this review to be translated in German.

Verein Deutscher Ingenieure
Welding Committee

Shrinkage Distortion in Welding

A Review of the Literature to January 1, 1937

By W. SPRARAGEN* and G. E. CLAUSSEN**

This Report Is Prepared Under the Auspices of the Literature Division of the Engineering Foundation
Welding Research Committee

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SUMMARY

There are two major aspects of shrinkage in all types of welding, namely, shrinkage distortion and shrinkage stresses. By shrinkage distortion is usually meant the overall motion of parts being welded from the position occupied before welding to that occupied after welding. For example, the distortion of a weld made in two parts held everywhere with absolute rigidity during welding is probably zero, yet the shrinkage stresses in such a welded part may be high. On the other hand, the distortion of a completely stress relieved, or highly preheated weld between two parts free to move during welding may be large, whereas the residual shrinkage stresses may be zero.

Butt Welds—Shrinkage Perpendicular to Weld

The following factors affect the shrinkage perpendicular to the weld:

(1) Cross-sectional area of weld for a given thickness of plate—the larger the cross section the greater the shrinkage;

(2) Time of welding. This is closely coupled with heat input. The greater the time or heat input, the greater the amount of distortion. Normally bare wire would give less distortion than covered wire. In some instances, gas welding will give less, and in other cases greater distortion;

(3) The angle of scarf is not nearly as important as "free-distance" or spacing between roots in causing distortion perpendicular to the weld;

(4) The greater the thickness of plate the greater the amount of distortion;

(5) The step-back procedure of welding lessens the amount of distortion;

(6) Peening properly used is effective in reducing the amount of distortion;

(7) Vertical layer method in arc welding gives less distortion than horizontal layer, or weave procedures.

Butt Welds—Angular Distortion

(1) Angular distortion of V joints free to move is practically independent of the thickness of plate, but increases with the number of layers.

(2) Angular distortion is greatest in V welds, next in U, and least in X welds.

(3) Angular distortion may be practically eliminated in X or double-U welds by welding alternately on both sides in multi-layer welding.

(4) Angular distortion may be controlled by peening of every layer to a suitable extent.

(5) Time of welding and size of electrode have an important bearing—(total heat input).

Butt Welds—Spacing Allowance

The average spacing allowance per foot of weld seam in sheet metal is dependent on the process, the speed of welding, and the heat input.

Fillet Welds

Transverse shrinkage (perpendicular to the weld):

(1) increases with size of weld and heat input;

(2) if weld is intermittent, shrinkage is proportional to the length of the weld;

(3) is less for bare as compared with covered electrodes;

(4) may be decreased materially by suitably choosing sequence of welding and peening;

(5) shrinkage is less for a lap joint than for a V butt weld.

Longitudinal shrinkage (parallel to the weld):

(1) increases with size of weld and heat input;

(2) is proportionally greater in continuous welds as compared with intermittent welding.

Ageing at room temperature after long periods may affect the amount of distortion.

Angular distortion is reduced by preheating, and by suitably arranging the sequence of welding, e.g. staggering.

Structures

The following are typical examples reported:

(1) Arc welding shortens plate girders about 0.1%;

(2) End shortening of arc welded latticed trusses is about 0.05%;

(3) Welded ships shrink 0.03 to 0.4% in length.

*Secretary, Welding Research Committee.
**Research Assistant, Welding Research Committee.

Shrinkage Distortion in Welding

Introduction

THERE are two major aspects of shrinkage in all types of welding, namely, shrinkage distortion and shrinkage stresses. By shrinkage distortion is usually meant the overall motion of parts being welded from the position occupied before welding to that occupied after welding. For example, the distortion of a weld made in two parts held everywhere with absolute rigidity during welding is probably zero, yet the shrinkage stresses in such a welded part may be high. On the other hand, the distortion of a completely stress relieved, or highly preheated weld between two parts free to move during welding may be large, whereas the residual shrinkage stresses may be zero. Since the distortion caused by welding is, in general, unrelated to the residual shrinkage stresses, the two phenomena are dealt with in separate reviews. The present review consists of a summary of the shrinkage distortions, apart from stresses, reported in the literature for all types of welding followed by an outline of existing theories of distortion.

Butt Welds—Shrinkage Perpendicular to Weld

Malisius[1] obtained the results shown in Figs. 1 and 2 for shrinkage perpendicular to the direction of welding. He did not mention the length and composition of the plate, nor the speed and sequence of welding. The shrinkage is the shortening of the total distance between the sides of the plates opposite the weld. The results show that shrinkage perpendicular to the weld is directly proportional to the cross-sectional area of the bead (approximately $^1/_{30}$ inch per sq. in. of weld cross section), and increases with increase of heat supplied by the welding process.

It is unfortunate that details of Malisius' procedure are not available because other investigators are not in good agreement with him. For example, Lottmann,[2] using manual welding with bare electrodes in a 90° V, found the shrinkage shown in Table 1. The plates were free but lightly tacked at both ends, the weld was 20 inches long, and the shrinkage given in the table is the average of measurements at both ends made by means of Zeiss dial gages. There was little difference between the readings at both ends. The shrinkage increased approximately in direct proportion with the time of welding the joint. The shrinkage responded much more

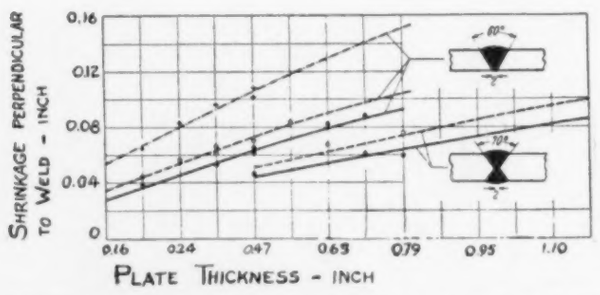

Fig. 1—Shrinkage Perpendicular to the Weld in Welded Butt Joints, 0.08-Inch Root Spacing

Calculated	Measured	
———	0	arc welding, bare electrodes
— — — —	0	arc welding, covered electrodes
— . — . —	x	oxyacetylene welding. Malisius[1]

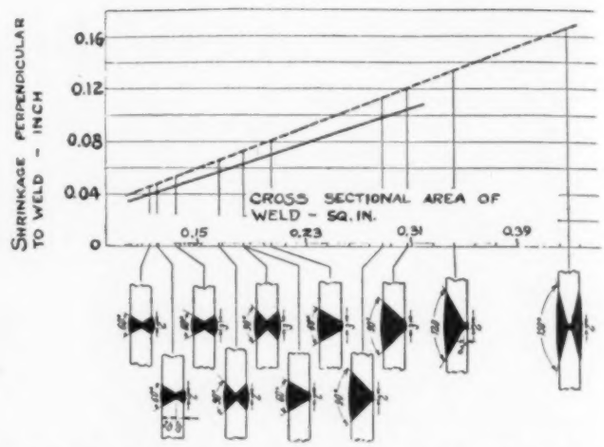

Fig. 2—Shrinkage Perpendicular to the Weld in Welded Butt Joints, 0.08 or 0.12-Inch Root Spacing

——— arc welding, bare electrodes
— — — — arc welding, covered electrodes. Malisius[1]

to an increase of 100% in root spacing than to a similar increase in plate thickness.

Table 1—Shrinkage Perpendicular to Butt Weld. Lottmann[2]

Plate Thickness, Inch	Number of Continuous Passes	Shrinkage, Inch 0.20-Inch Spacing	Shrinkage, Inch 0.43-Inch Spacing
0.20	1	0.049	0.067
0.39	1	0.051	0.069
0.39	2	0.055	0.073

The results of Matting[3] on the effect of plate thickness and angle of scarf shown in Table 2, also fail to offer quantitative verification of Malisius' work. The effect of welding procedure on shrinkage perpendicular to V butt welds in 0.24-inch plate 7 inches long tacked at both ends, using a 0.16-inch electrode, 120 amps., is

Table 2—Shrinkage in Inches Perpendicular to Butt Weld. Matting[3]

Angle of Scarf, Degrees	Plate Thickness, Inch 0.35	0.51	0.63	0.71	0.95
60	0.043	0.061	0.071	0.075	0.097
75	0.047	0.063	0.083	0.087	0.103
90	0.051	0.071	0.083	0.087	0.105

shown in Table 3. The back step procedure gave the least shrinkage.

Table 3—Effect of Welding Procedure on Shrinkage Perpendicular to Butt Weld. Matting[3]

Procedure	Shrinkage, Inch
Welded in one pass ⟶	0.055 (with tack in middle = 0.038 in.)
1 → 2 →	0.058
2 → 1 →	0.043
1 → ← 2	0.057
1 ← 2 →	0.047

Table 4—Shrinkage Perpendicular to Butt Weld. Shaw[4]

Welding Data	Rod Size, Inch	Welding Time Min.	Root Spacing Inch	Plate Thickness Inch	Free or Restricted	Shrinkage, Inch Start	Shrinkage, Inch Finish
Oxyacetylene:							
25 lb. pressure	1/4	5.33	1/8	1/2	Free	0.040	0.023
20 " "	1/8	4.5	3/32	1/4	Free	0.032	0.010 (expansion)
20 " "	1/4	5.04	1/8	1/2	Restricted	0.049	0.057
20 " "	1/8	3.8	3/32	1/4	Restricted	0.039	0.054
Metal Arc, 4 runs 95–150 amps.	8 gage	7.6	1/8	1/2	Free	0.057	0.114
Metal Arc, 2 runs 110–115 amps.	10 gage and 6 gage	2.98	1/16	1/4	Free	0.049	0.036

The effect of plate thickness as well as of restricted expansion on V welds was investigated by Shaw,[4] Table 4. The welds were 6 inches long; the angle of the V was 60° except for the 1/2-inch arc weld, which was 70°. The length perpendicular to the weld over which the shrinkage was measured was about 11 inches. From 1/2 to 1 1/2 min. chipping was done after each layer of the metal arc weld. Gas welded deposited metal had a tensile strength of 63,000 psi. It was observed that immediately after welding was completed the gas welded specimens tended to show less contraction (even an expansion) than the arc welded specimens.

Shaw's painstaking results on oxyacetylene welds are not in agreement with the statement by Bainbridge[5] that a four-fold increase in the volume of oxyacetylene weld metal increased the shrinkage across the weld from 0.01 inch to 0.11 inch. The latter gives no experimental details. Madsen[6] states that the shrinkage perpendicular to U, V or X welds is about 18% of the average width of the cross section of the weld, regardless of wire gage and welding speed.

The effect of restraint and cold peening on arc welds with electrodes having a tensile strength of 71,000 psi, elongation 25% has been investigated by Sarazin.[7] The welds were 10 inches long, 75° V, 0.08 inch root spacing in soft steel 0.55 inch thick, having a yield point of 36,000 psi, tensile strength 51,000 psi. Measurements were made directly by a steel scale with the aid of a microscope eyepiece (20×). In a preliminary test Sarazin found that the shrinkage across a joint varied with the gage length. For example, for a gage length of 1.18 inches (distance across face of V was 0.86 inch), the total shrinkage contraction after a four-pass weld with reverse run (plates free to move) was 0.043 to 0.071 inch depending on the location of the gage length along the length of the weld. For gage lengths of 4.3 to 9 inches the total shrinkage contraction was only 0.020 to 0.055 inch. The difference was not explained but a gage length of about 1 1/2 inches (3/4 in. on each side of the center line of the joint) was chosen for the remaining tests. A weld in plates free to move contracted 4 to 6% in 1 1/2 inches across the joint; a similar weld in plates rigidly welded to a base plate 3.15 inches thick contracted 2.3 to 3% in 1 1/2 inches across the joint. By cold peening each layer of the rigidly clamped weld the contraction due to the previous bead was counterbalanced, the completed weld showing an expansion of nearly 2%. In the absence of information on the peening procedure adopted by Sarazin, his results must be accepted simply as an indication of what may be accomplished by peening.

Diagrams similar to Sarazin's showing distortion at various points along free and rigid V butt welds, and patch and plug welds as well, were obtained by Owens[8] in 1923. The welds were 12 to 24 inches long in 1/2-inch plate. The radial and tangential shrinkage distortions caused by "patch" welding were also measured by Hodge,[8a] whose "patch" was a flat circular plate 1 5/8 inches thick, 24 inches diameter welded (no details) in the center of a plate of the same thickness and 8 feet square. Strains across the weld were not measured. There was everywhere radial shrinkage, which was maximum (0.0055 inch per inch, 2-inch gage length) close to the weld. There was also tangential shrinkage in the disk but tangential expansion in the plate.

Jennings[9] investigated the shrinkage contraction perpendicular to 60° X butt welds, Fig. 3, in rigidly clamped plates 1 1/2 inch thick. Unlike other investigators, he did not measure contraction across the joint, but on 1-inch gage lengths on either side of the weld itself, one point of the gage length being only 1/2 inch away from the edge of the weld. For this reason the contractions he measured, besides being made up of elastic and inelastic action, were not indicative of the total distortion across the joint. Nevertheless, it was assumed that the measurements actually represented the overall shrinkage. The results for the six specimens investigated showed that, on the particular gage lengths chosen, the vertical layer method of welding gave less distortion than horizontal layer or weave procedures. Peening (no details) reduced the distortion on that gage length by about 30% with respect to unpeened. The welds were 3 inches long and were made with 5/32-inch bare low-carbon electrodes, 100 to 150 amps.

Michaud[10] compared the shrinkage across two oxyacetylene welds in rigidly gripped mild steel plates, 0.39 inch thick, using a gage length of 1 3/4 inches.

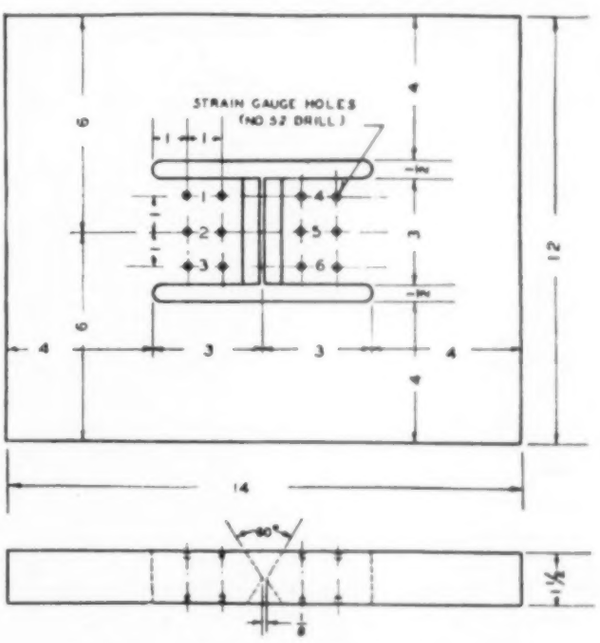

Fig. 3—Specimen for Measuring Welding Distortion. Jennings[9]

Welds made by the customary right-hand method had a contraction across the joint of 0.079 inch; the corresponding value for welds made by the two-torch vertical method was 0.066 inch (no details). Like Sarazin, Michaud found that the total shrinkage across the joint was 5 to 10% less on a gage length of 5 inches than on $1^3/_4$ inches, for his rigidly gripped welds. Eskilson[11] reports shrinkage measurements on oxyacetylene tack and butt welds and compared them with similar measurements on arc welds. However, the welding procedures he adopted were not uniform and his results are ambiguous.

Informative experiments on oxyacetylene welding strips of plain-carbon and chromium-molybdenum deep-drawing steels were made by Müller.[12] The strips were 0.39 x 0.04 inch and were butt welded in a single pass. Three tests were made. (1) Strips without constraint elongated 0.025 inch during welding and shrank to the original length after cooling. (2) Strips gripped so that no expansion could occur contracted 0.025 inch after welding. (3) Strips gripped so that neither expansion nor contraction could occur contracted 0.004 inch after release.

Plots of deformations parallel and perpendicular to butt welds as shown by extensometer or other measurements on gage lengths before and after welding are given by Mies,[13] Bierett,[14] Theisinger[15] and Reinhard,[16] in addition to those discussed above. Mies showed that within the heat-affected zone of gas welds as revealed by temper colors, permanent local deformations of $\pm 0.051\%$ may be expected. Elsewhere the permanent deformation may be 0.004%. Reinhard found in oxyacetylene welding a slot 12 inches long (90° V, 0.08-inch root spacing) in the center of a mild steel plate 60 inches long, 48 inches wide, 0.55 inch thick, that there is permanent shortening of 0.03 to 0.08% parallel to the welded slot and 0.01 to 0.15% in $1^5/_8$ inches perpendicular to the slot, depending on location of the gage length. In free continuous butt welds there was an actual lengthening parallel to the weld of 0.01 to 0.02%. Lance Martin's measurements[17] on a restrained arc-welded joint showed that practically all of the permanent shrinkage perpendicular to the weld occurred within 1 inch of the weld.

Butt Welds—Angular Distortion

The angular distortion of a free butt joint during welding, as Koch showed, is related to the shrinkage perpendicular to the weld by the formula

$$Z = X + \frac{S}{2} \operatorname{Tan} \varphi \quad \text{(see Fig. 4)}$$

Koch's measurements[18] of Z, X and φ as a function of number of layers are summarized in Table 5.

The welds were 7 inches long; each plate was $4^3/_4$ inches wide. The root spacing (60° V weld) was 0.12 inch. The welds were made by A.C. arc process with covered electrodes, 0.16 and 0.20 inch diameter. From the standpoint of angular distortion the number of layers should be small.

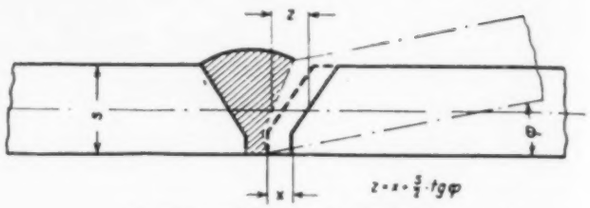

Fig. 4—Angular Distortion of a Butt Weld

φ — angular distortion
x — shrinkage perpendicular to weld without angular distortion
z — total shrinkage perpendicular to weld.

Angular distortion in V, U and X welds in 1-inch plate, as observed by Höhn,[19] is shown in Table 6. Coated electrodes, 0.13 (first run) to 0.28 inch diameter, were used, the currents varying from 140 (0.13 inch) to 400 amps. (0.28 inch). The root spacing was 0.08 inch and the last run in the U and V joints was a root run. The plate had a tensile strength of 59,000 to 71,000 psi, elongation 22 to 26%. In addition to the obvious conclusions to be drawn from Table 6, it was found that the angular distortion increased after each successive run on the same side of a V and that the root layer should be welded before the joint is completely filled in. Although the scarf angle was not stated by Höhn, it is true, as J. H. Deppeler (private communication, June 1937) pointed out, that a straight gap butt weld with parallel sides produces less angular distortion than the same amount of weld metal deposited in a 75° V butt weld.

The effect of current and size of electrode on the angular distortion of a 60° V butt weld in 0.59-inch mild steel has been determined by Madsen.[6] He found the angle φ was 4° after welding with 0.28-inch covered electrodes, 350 amps. A.C., but 10° after welding with 0.16-inch covered electrodes, 175 amps. A.C. Madsen observed that angular distortion could be controlled within wide limits by cold peening every layer to a suitable extent. In agreement with Madsen, Ross[20] found that angular distortion with high-current welding (4-gage electrode, 300 amps.) is only $^1/_3$ that for welds of the same size (no details) made with smaller electrodes in a larger number of passes.

The angular distortion of single-and double-U arc welds in $1^1/_2$-inch flange steel plates 36 inches long, 18 inches wide was measured by Kinkead.[21] Peening reversed 50% of the angular distortion produced by a bead. Angular distortion could be reduced to zero by depositing series of two beads alternately on each side of a double-U joint. Peening has often been used in practice to correct shrinkage distortion. For the same type of 60° V butt joint in 0.39-inch mild steel, Keel[22] observed an angular distortion of 4° 35′ for an arc weld (three layers, no details) and 0° 20′ for a gas weld (one layer). The welds were 4 inches long. Lessel[23] found that the angular distortion of a plate during welding a milled-out defect consisted of two parts: a tilt up during welding, and a tilt down during cooling. The tilt down was about $^1/_3$ the total tilt up, using coated electrodes. If the length of the milled-out section was less than about 20% of the length of the weld no appreciable angular distortion was observed.

Table 5—Angular Distortion and Shrinkage Perpendicular to a Butt Weld in 0.47- and 0.71-Inch Plate. Koch[18]

	Z, Inch		X, Inch		ϕ, Degrees	
Number of Layers	0.47-Inch Plate	0.71-Inch Plate	0.47-Inch Plate	0.71-Inch Plate	0.47-Inch Plate	0.71-Inch Plate
3	0.067	0.077	0.064	0.071	1	1
4	0.070	0.083	0.064	0.075	2	2
5	0.073	0.089	0.057	0.073	3.5	3
6	...	0.095	0.053	0.066	...	4.5
8	...	0.120	0.047	0.055	...	8

Table 6—Angular Distortion in Butt Welds. Höhn[10]

	V	V	U	U	U	U	X[A]	X[B]	X[A]
Distance between plates at face, inch	1.10	1.14	0.59	0.59	0.75	0.71	0.67	0.67	0.67
Diameter of electrode, inch	0.13–0.20	0.13–0.28	0.13–0.16	0.13–0.24	0.13–0.28	0.13–0.28	0.13–0.24	0.13–0.24	0.13–0.24
Free or Clamped	Free	Clamped	Free	Free	Free	Clamped	Free	Free	Clamped
Number of layers	11	8	10	8	7	8	7	9	6
φ(max), degrees (after layer no.)	10° 30′ 10	0 7	8° 0′ 9	5° 10′ 7	4° 50′ 6	1° 10′ 7	2° 50′ 3	−1° 20′ 4	0° 35′ 3
φ(final), degrees, after last layer	9° 30′	0	7° 40′	4° 30′	3° 40′	0° 50′	−1° 45′	0	0

[A] = one half of X was completely filled before the other.
[B] = layers deposited alternately in both halves.

Butt Welds—Shrinkage Parallel to Weld

Lottmann[2] found that the shrinkage parallel to a weld was 0.03% of the length of the weld for 90° V butt welds (0.16-inch electrodes) in mild steel 0.30 to 0.47 inch thick. The width of the plates was not stated. For rigidly gripped 75° V welds, 10 inches long, in mild steel, $^1/_2$ inch thick, made in four passes with root run with electrodes having a tensile strength of 71,000 psi, Sarazin[7] found a shrinkage contraction parallel to the weld of 0.47%, which was reduced to 0.24% by cold peening each layer. The cold peening itself caused a reversal of the contraction of each bead of only 0.0001 to 0.05%, but had a considerable effect on the over-all shrinkage.

Butt Welds—Spacing Allowance

The average spacing allowance per foot of gas welded seam in sheet metal $^1/_8$ inch and less in thickness, according to Sheet Metal Worker,[24] is:

$^1/_4$ to $^3/_8$ inch per foot for steel
$^3/_{16}$ inch per foot for brass, bronze and copper
$^1/_5$ inch per foot for aluminum
$^5/_{16}$ inch per foot for lead
$^3/_8$ inch per foot for Monel Metal

The Director of Naval Construction of the British Admiralty[25] recommends the following values for spacing between 60° V arc welds in ship steel:

$^1/_{16}$ inch for plate up to and including $^1/_4$ inch thick (10 lb.-ft.^{2})
$^3/_{32}$ inch for plate up to and including $^3/_8$ inch thick
$^1/_8$ inch for plate over $^3/_8$ inch thick

Matting[3] found that there is approximately 0.0275-inch closure of gap for every inch of seam in untacked 0.24 inch mild steel plate welded with 0.16-inch electrodes, 120 amps. Tacking decreased the closure but no quantitative data are given.

Measurements of the change of distance between plates 20 to 40 inches long, 0.32 inch thick, during oxyacetylene welding were made by Holler[26] and Melhardt.[27] With plates parallel and untacked before welding, the latter found that the opening at the end first grows wider (from 0.08 inch to 0.32 inch), then grows narrower (to 0.16 inch) as welding proceeds. The usual rule for oxyacetylene welding is to leave an opening of 2 to 4% of the length of the weld, which happens to agree closely with the value 2.56% found by Whittemore[28] in 1911. But Melhardt points out that the rule does not apply to high speed, righthand welding. The percentage is somewhat less for arc welding, according to G. W. Plinke (private communication, June 1937). The effect of velocity of welding on spacing allowance is strikingly illustrated by Townshend.[29] Steel plates 10 ft. long, $^1/_4$ inch thick, $^1/_8$ inch apart before welding, were welded on the 10 ft.-edge. The free ends crossed each other when No. 10 gage covered electrodes were used, but when No. 4 gage electrodes of the free flowing type were used with three times the amperage, the ends actually opened up $^3/_8$ inch.

The length of bead obtained from a single electrode has an important influence on spacing allowance, as shown by W. D. Chapman[30] in Table 7.

Table 7—Change of End Spacing in Butt Welds. W. D. Chapman[30]

Size of Electrode	Length of Run per Electrode, Inch.	Current Amps.	Time Seconds	Decrease in Gap, Inch
8 gage	7	140	160	0.25
8 gage	14	140	87	0.13
8 gage	21	140	65	0.11
6 gage	15	170	82	0.16
6 gage	21	165	67	0.06
6 gage	30	185	45	0.04

Each plate was 12 x 4 x $^1/_4$ inch, welded 7 inches along the 12-inch edge. The net length of each electrode was $16^3/_4$ inches. The decrease in gap is the closure of the free ends after welding the 7-inch bead.

Fillet Welds

Rossell[31] reports the results of experiments at Fore River on shrinkage distortion due to welding the legs of T irons 3 x $^3/_8$ inch x 2 feet long, at intervals of 13 inches across the width of medium steel plates 12 feet long, 2 feet wide, $^3/_8$ inch thick. The welds were $^1/_4$-inch leg made with $^5/_{32}$-inch Fleetweld covered electrodes, 30 volts (arc), 130 amps. The following formula was evolved.

Transverse shrinkage (perpendicular to weld) = 0.03 (C − 0.2) inch,

where C is the average continuity, equal to the length of a section of intermittent welding divided by the distance between centers of successive sections, neglecting craters. (C = 1 for continuous fillets; if C = 0.2 or less the shrinkage is negligible.) Transverse shrinkage was decreased 25% by using bare electrodes, was decreased 50% by wandering sequence, and varied in direct proportion to arc energy and size of weld. Intermittent welds filled in subsequently to make a continuous weld had the same transverse shrinkage as a straight continuous fillet. The average transverse shrinkage for continuous welds made with bare electrodes ($^1/_8$-inch electrode, 130 amps.; $^3/_{16}$-inch electrode, 190 amps.), reported by Mare Island Navy Yard, was: T joint, 0.054 inch; Lap joint, 0.059 inch; Single strap butt joint, 0.064 inch; Lap joint with T joint above it, 0.075 inch. Plate thickness was not stated. Longitudinal shrinkage (parallel to the weld) was 0.005 inch per linear foot of joint (equivalent to 0.04%) for free joints, and $^1/_5$ of this value for relatively rigid joints.

Wallin and Schade[32] made similar measurements, Table 8 (averages of several hundred tests) using a 6-inch tram for transverse shrinkage (no shrinkage occurred outside the 6-inch band spanning the joint) and a 10-foot tram for measurement of longitudinal shrinkage along the joint. These results appear to be the same as those quoted by Rossell as originating at Mare Island, but Wallin and Schade give no welding details.

Table 8—Longitudinal and Transverse Shrinkage in Ship Joints. Wallin and Schade[32]

Type of Joint	Transverse Shrinkage Inch (in 6 Inches)	Longitudinal Shrinkage Inch in 10 Feet
T joint (fillets on both sides)	0.054 (continuous); 0.01 (intermittent)	0.01 (continuous)
Joggled lap joint	0.059 (without T) 0.075 (with T)	0.03 (with T)
60° V butt joint with single strap, with or without intermittent or continuous T joint on the strap.	0.084	...

The plates were $^1/_8$ to $^3/_8$ inch thick, plate thickness having practically no effect on results because the joints were geometrically similar, the size of the weld being proportional to the thickness of the joint. The T joint on the joggled lap joint was intermittent welded. Wallin and Schade conclude that the lap joint shrinks less than the butt joint, particularly if neither has a backing-up member.

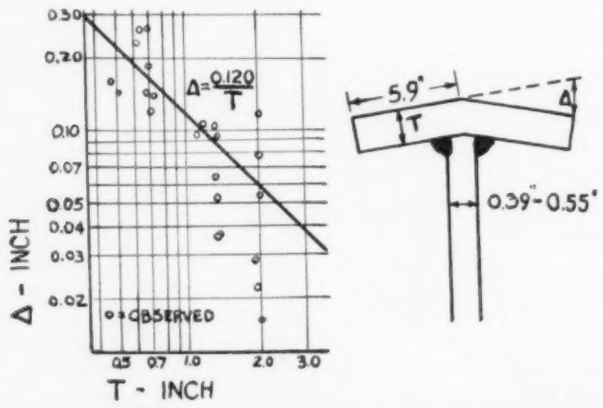

Fig. 5—Angular Distortion of Mild Steel Flange Plates in Arc-Welded Plate Girders
Throat thickness of fillet weld = 0.24 inch. Reinhold and Heller.[33]

The results of Lottmann[2] (German Navy) shown in Table 9 are in fair agreement with Wallin and Schade's values. Lottmann's mild steel plates were $^1/_8$ to $^3/_4$ inch thick, arc welded (no details).

The angular distortion of mild steel flange plates in welded plate girders caused by continuous fillets has been studied by Reinhold and Heller,[33] whose results are given in Fig. 5. The welds were made manually or by machine using mild steel electrodes (no details). The empirical formula can hardly be said to be successful. A similar formula has been suggested by Paton.[34] Dorrat[35] found that shrinkage contraction in T joints perpendicular to the weld was least for unmachined plates without gap and greatest for the opposite condition (no details). Keel[22] found that in oxyacetylene welding end fillets the angular distortion was 0° 20′ whereas in arc welding the distortion was 2° 25′ (0.39-inch mild steel). Sonderegger[36] investigated the effect of preheating on the angular distortion of angle iron (one leg $4^3/_4$ x $1^3/_4$ inches; the other leg $5^3/_4$ x $1^3/_8$ inches) end fillet arc welded to a flat plate $1^3/_{16}$ inch thick. Without preheating the angular distortion was 8° 45′; preheating to 300° C. reduced the distortion to 4° 20′ (no details).

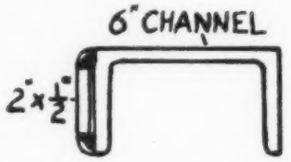

Fig. 6—Type of Specimen Used by Owens[37] for Measuring the Average Pulling Together of Faying Surfaces Due to Fillet Welding

Measurements of the extent to which fillet welding draws two plates together have been made by Owens.[37] Strips of steel 2 inches wide and $^3/_8$, $^1/_2$ and 1 inch thick were single or double-fillet welded by metal arc (no details) to the leg of 6- or 12-inch channels, Fig. 6. An intermittent fillet pulled the strip toward the channel to the extent of 0.005 inch per linear inch of joint for $^3/_8$-inch strips, and 0.010 and 0.014 inch per linear inch (continuous fillet) for $^1/_2$- and 1-inch strips, respectively. A second layer compressed the first layer 0.0003 inch per linear inch in all thicknesses.

Distortion measurements on fillet welds by Höhn[19] are summarized in Fig. 7. Originally the distances x_1 and x_2 were 0.394 inch; after welding (no details) $x_1 = 0.374$ inch, $x_2 = 0.381$ inch. The shrinkages observed across the gage lengths w were:

$w_1 = 0.015$ inch	$w_4 = 0.015$ inch
$w_2 = 0.032$ "	$w_5 = 0.005$ "
$w_3 = 0.031$ "	$w_6 = 0$

Lawson[38] recorded the welding distortion along about 70. 2-inch gage lengths on both sides of a plate (0.70–0.85 C, 40 x 7 x $^1/_2$ inch) to which two other plates (0.10 C, 32 x 3 x $^1/_2$ inch, each) were metal arc welded with $^5/_{32}$-inch electrodes, 18 volts (arc), 175 amps. The characteristic distortion for a given gage length was determined by the nearest weld. The average distortion was 0.0005 inch per inch, mainly elongation. After resting at room temperature for one year, the plates were measured again; all gage lengths were found to have contracted to the extent of 0.002 to 0.003 inch per inch. No explanation was offered.

Table 9—Longitudinal and Transverse Shrinkage in Ship Joints. Lottmann[2]

Type of Joint	Transverse Shrinkage, Inch 0.12–0.32-Inch Plate	0.32–0.47-Inch Plate	Longitudinal Shrinkage, Inch in 10 Feet	
T joint, intermittent fillets	0.010–0.014	0.008	0.024	(0.02%)
T joint, continuous fillets	0.04	0.030	0.042	(0.035%)
Single reinforcing strap with T joint having intermittent fillets superimposed	0.04	...	0.042	(0.035%)
Lap joint (double fillets)	0.055–0.059	0.060	...	...
V butt joint	0.071	0.065	0.036	(0.03%)
X butt joint	...	0.075	...	...
V butt joint with T joint superimposed	0.079	0.11	...	...

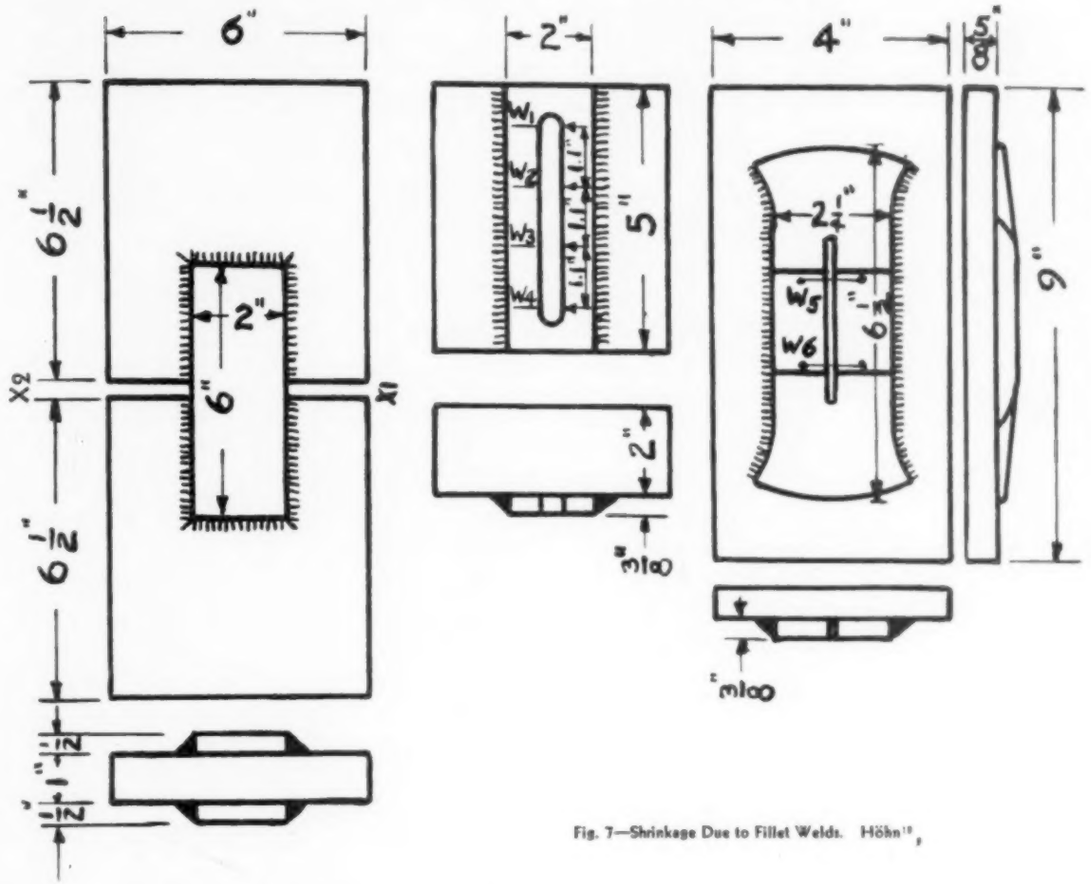

Fig. 7—Shrinkage Due to Fillet Welds. Höhn[19]

The shrinkage distortion of a plug or rivet weld (1.18 inch diam.) in 0.39-inch mild steel was investigated by Sarazin[7] by means of a strain gage. The weld was made with one pass of a No. 8 gage and one pass of a No. 6 gage electrode. The shrinkage across the weld was over 0.01 inch per inch. The distortion on the reverse side of the lower plate was of the same sign as, but much less than, that of the upper, welded plate.

Tubes

Rechtlich[39] found that the shrinkage of oxyacetylene welded aircraft tubing perpendicular to a rigidly clamped butt joint was independent of the outside diameter of the tube from 0.32 to 2 inches, and of wall thickness, 0.02 to 0.08 inch, Table 10. The welding rod contained 0.07 C, 0.17 Mo, 2 Ni. If the tubes were not clamped, the shrinkage was nearly zero for tubes 0.32 inch o.d., but was nearly the same as for clamped for tubes 2 inches o.d. The values for the rigid condition were found to apply closely to the usual aircraft fuselages.

Table 10—Transverse Shrinkage of Rigidly Clamped Butt-Welded Aircraft Tubing. Rechtlich[39]

Composition of Tube, %				Axial Shrinkage Perpendicular to Weld, Inch per Weld
C	Mn	Cr	Mo	
0.3	0.5–0.6	...	...	0.022
0.6	0.5–0.6	...	...	0.026
0.25–0.35	0.4–0.6	0.8–1.1	0.15–0.25	0.035

Beads of Weld Metal

Shrinkage distortion due simply to depositing a bead of weld metal on the surface of a flat mild steel plate has been investigated by Sarazin,[7] using a special direct-reading "tensiometer" (like a Berry gage) accurate to $^1/_{25,000}$ inch on a gage length of 1 inch. The results are shown in Table 11.

Table 11—Shrinkage Distortion Caused by a Bead of Weld Metal. Sarazin[7]

Plate Thickness, Inch	Number of Beads	Maximum Shrinkage, Inches per Inch Perpendicular to Bead	Parallel to Bead
0.39	1	0.0115 (0.0096)	0.0024 (0.0010)
0.79	2	0.0100 (0.0091)	0.0025 (0.0004)

Welding was done with 0.16-inch electrodes, 140 amps. The distortion on the reverse side of the 0.39-inch plate was somewhat less than, but of the same nature as, that on the bead side. In the 0.79-inch plate there was an expansion under the bead instead of a contraction, showing that cross section has an effect on distortion. The values in parentheses in Table 11 apply to similar specimens with a heated strip instead of a bead of weld metal. The method of producing the locally heated area involved passing an electrode (100 amps.) over the surface without depositing a bead. Sarazin concluded that the bead of deposited metal is responsible for 30% of the distortion, the heating and cooling of the plate accounting for the remainder.

The effect of the ratio of cross section of bead to that of plate, as determined by W. D. Chapman,[30] is shown in Table 12. The net length of an electrode was $16^3/_4$ inches. There was an angular distortion concave to the bead in both directions, greater perpendicular to the bead than parallel, and increasing with size of bead. Experiments with multi-layer beads showed that the second layer of a deposited pad causes relatively more

Table 12—Shrinkage Caused by a Single Bead of Weld Metal Deposited in the Middle of a Plate. W. D. Chapman[30]

Size of Plate, Inch	Number of Beads	Size of Electrode	Inches of Bead per Electrode	Current Amps.	Ratio of Section of Plate to Bead	Shrinkage Distortion Parallel to Bead Inches in 6 In.	Shrinkage Distortion Perpendicular to Bead Inches in 3 In.
6 x 3 x 1/2	1	10 gage	8.7	110	62:1	0.05	0.03
6 x 3 x 1/2	1	8 gage	11	140	54:1	0.06	0.05
6 x 3 x 1/2	1	6 gage	13.4	180	45:1	0.08	0.05
6 x 2 x 3/16	1	10 gage	6.2	110	11:1	0.09	0.03

distortion than the first. Shenstone,[40] in 1920, measured the contraction of square and round mild steel bars 10 inches long, 1 sq. in. cross section, caused by depositing 3/16-inch bare Swedish iron electrodes on the surface. An expansion of 0.062 in. while hot and a contraction of 0.016 inch when cold was observed in both round and square bars having 11 oz. of metal deposited over lengths of 5 1/4 and 4 3/8 inches, respectively. According to Hochheim,[41] there is a shrinkage of 0.012 to 0.020 inch perpendicular to a bead 0.24 to 0.32 inch wide (no details). Virgili[42] uses a special device for measuring distortion parallel to a bead deposited on a grooved bar by oxyacetylene process. The movement of two pointers fixed to the ends of the bar indicates the distortion.

Rosenthal[83] found that a mild steel bar 0.79 x 1.58 inches, supported on a span of 19 inches upon which a longitudinal bead of arc-weld metal 12 inches long (no details) was deposited in the center portion, deflected to an extent depending on the rate of supply of energy to the bar. Table 13 shows that as the rate of advance of the electrode is increased at constant amperage the permanent deflection increases to a maximum, then decreases.

Table 13—Permanent Deflection of Freely Supported Mild Steel Bar Caused by Bead Deposited on Surface. Rosenthal[83]

Rate of Supply of Energy to Electrode, Ampere Minutes per Inch of Bead	Permanent Deflection, Inch
0	0
12.7	0.055
25.4	0.106
38.1	0.157
50.8	0.169
63.5	0.146
76.1	0.102

Shrinkage Distortion in Structural Welding

More or less complete measurements of shrinkage distortion have been made on a number of structures. The distortion during the welding of angle irons to flat plates has been studied by W. D. Chapman,[30] Table 14.

Table 14—Arching of Angle Iron Welded to Flat Plate. W. D. Chapman[30]

Electrode	Gage of Electrode	Inches of Bead per Electrode	Welding Procedure	Distortion of Middle Ordinate in 2 Ft. Lengh
Bare	10	7	Continuous	0.16 inch
Fluxed (New Era Extra)	10	7	Back step; 4 steps	0.05
"	6	13	Continuous	0.16
"	10	9	"	0.18
"	10	7	Continuous, but with tacks at 9-inch intervals	0.07

The 1 1/2 x 1 1/2 x 3/16-inch angles were welded to the center of a 4 x 5/16 inch plate. Pre-distortion, as well as backstep welding is successful. It was only necessary to pre-distort 1/2 to 1/3 of the amount of distortion if the parts were free.

Moon[43] found that pre-distortion applied to T and plate produced greater permanent distortion than without pre-distortion. The T iron was 3 x 3 x 3/16 inch and was welded to a flat plate 12 x 1/2 inch, 6 feet long by intermittent fillet welds, 6-inch weld, 6-inch gap. If the weld was made from one end to the other with a welder on each side of the T, the plate bent concavely about 1/8 inch and the edges of the plate, tending to fold about the T, came up 1/8 inch. Staggering the welds and starting from the middle of the T prevented most of the distortion.

The distortion occurring on fabricating T beams 10 feet long from a web plate (7 x 0.47 inch) and a flange plate (7 x 0.63 inch) by continuous fillet welds (automatic arc, electrodes with TiO_2 in coating; first layer 0.16-inch electrode, 190 amps.; 40 feet of weld per hour; second layer, weaved, 0.20-inch electrodes, 220 amps.; 33 feet of weld per hour) has been measured by Paton and coworkers.[44] The sequence of welding is shown in Fig. 8. Measurements of the distortions "f" at 11 equally spaced sections along the welded beam are shown in Table 15. By cold hammering the web to an initial curvature of f = 0.41 inch the investigators found no distortion after welding. With an initial curvature of the web of $f = 1\frac{1}{4}$ in., the curvature after welding was $f = 1.25 - 0.41 = 0.84$ inch. The center of gravity axis of the beam was 0.55 inch from the bottom of the flange. Paton and coworkers found that for plates of the same cross section, f is proportional to the square of the length for beams 6 to 20 feet long. Bergfeld[45] shows that large distortions may be obtained in the welding of a flange to a web even with the use of an hydraulic pump as corrective.

Reinhold and Heller[33] found that arc welding shortens welded plate girders about 0.1%, and Grove[46] reports 0.05% as the end shortening of arc-welded latticed trusses (40 feet long). Greger[47] gives some shrinkage measurements on roof trusses, Schroder[48] on a large welded frame, and Rechtlich[39] on oxyacetylene welded tubular fuselages. Distortion allowances for welding 8-inch channels are given by Ross.[20] Bruff[49] found that sequence and speed of arc welding had little effect on final distortion of I beams with plates fillet welded to the flanges. The maximum contraction was about 0.07%.

According to Dungan,[50] the shrinkage of welded ships varies from 0.0036 to 0.050 inch per foot of length (0.03 to 0.4%). Wallin and Schade[32] found that a welded vessel shrinks, whereas a riveted vessel usually expands. The shrinkage in the beam of a 118-foot ship was 5/8 inch

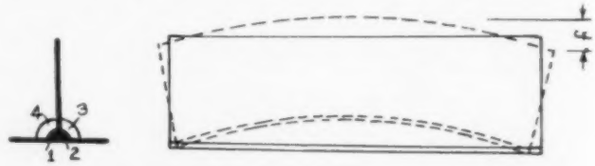

Fig. 8—Sequence of Welding T Beams. Paton and Coworkers.[44]

Table 15—Distortion in Inches of Welded T Beams. Paton and Coworkers[44]

Section	1	2	3	4	5	6	7	8	9	10	11
Flange	0	0.070	0.096	0.149	0.168	0.182	0.204	0.157	0.110	0.064	0
Web	0	0.125	0.076	0.259	0.297	0.411	0.411	0.289	0.243	0.188	0

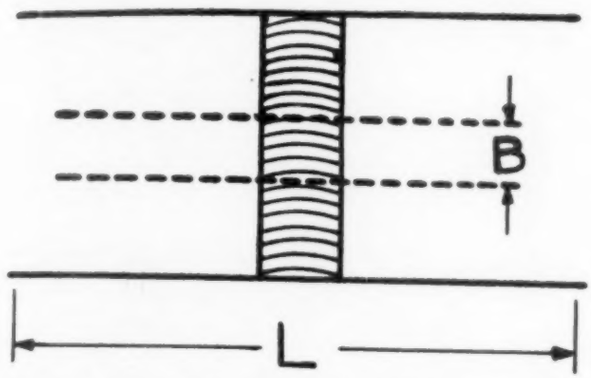

Fig. 9—Notation Adopted by Wortmann and Mohr[59] for Shrinkage Across a Butt Weld

in 26 feet (0.2%). The shrinkage of an oxyacetylene welded margin plate attachment common in marine construction is described by Shaw.[4] Similar data on an arc-welded tank with different weld cross sections are given by Lottmann.[2] Sehring[51] reports shrinkage allowances to be made when welding patches on beams and boilers, and Polson and Sinfield[52] give detailed results on arc-welded, open-end, mild steel boxes, 5/8-inch wall, 12 inches square. There was practically no distortion due to welding.

The welding distortion in large arc-welded pipe 65 inches diameter, 1/2-inch wall, has been studied by Jones,[53] who found that deformation is not uniform around the circumference. Tacking reduced the deformation in all directions 50% with respect to an untacked roll weld. Bondy[54] gives a few values of distortion of welded locomotive frames (welded bridge-type girder), and Hiemke[55] found that distortion due to arc-welded repairs on a large steel casting exceeded 0.020 inch in several cases. Distortion due to stress relief of the welded casting (4 hrs. at 900° F.) in no case exceeded 0.006 inch (gage length not stated). Detailed measurements of distortion on two types of carbon-arc welded rail joints of the joint plate type are given by Lawson,[56] and Crook and Dugan.[57] Miller[58] states that distortion in projection welding decreases as the time of welding is decreased.

Theoretical Aspects

Two different theories have been proposed to account quantitatively for shrinkage distortion in welding without reference to stress. The theory developed by Wörtmann and Mohr[59] is based on purely thermal factors. In making a butt weld the heat supplied by the electrode is considered uniformly distributed over a length L perpendicular to the weld, Fig. 9. The weight of steel in the strip, which is assumed to be heated from 20 to 700° C., is $G = L \cdot B \cdot D \cdot S$ where D is density and S is plate thickness. The heat supplied is $Q = g$, $k = C \cdot \Delta T \cdot G$ where g = weight of weld metal deposited per unit length (B) of weld, (gm./cm.),

k = heat in each unit weight (gram) of weld metal,
C = average specific heat of iron (20 to 700° C.), and
ΔT = temperature rise in the strip heated from 20 to 700° C.

If the average coefficient of thermal expansion is A, then the relative elongation of the heated strip is,

$$\Delta L = \frac{A \cdot g \cdot k \cdot L}{C \cdot G} = \frac{A \cdot g \cdot k}{C \cdot D \cdot S}, \text{ assuming } B = 1 \text{ cm.},$$

$$\text{since } \frac{\Delta L}{L \cdot A} = \Delta T.$$

On cooling the plate shrinks an equal amount, to which is added the shrinkage of the weld metal.

$$\Delta L = \frac{A \cdot k \cdot g \cdot 10^5}{C \cdot D \cdot S} + 0.0103F$$

where $0.0103 = A \cdot \Delta T$, F is the greatest breadth of the weld, and 10^5 is to balance units (cgs. system).

Since the shrinkage across the joint, expressed as percentage of breadth of weld, is

$$S_p = \frac{\Delta L \times 100}{F}, \text{ } F \text{ and } S \text{ being in mm.},$$

$$S_p = \left(\frac{220g}{F \cdot S} + 1.03\right)\%, \text{ for d.c. welding, electrode negative;}$$

$$S_p = \left(\frac{124g}{F \cdot S} + 1.03\right)\%, \text{ for d.c. welding, electrode positive;}$$

$$S_p = \left(\frac{141g}{F \cdot S} + 1.03\right)\%, \text{ for a.c. welding.}$$

These formulas are based on values determined by Wörtmann for the thermal output of electrodes.

A modification of Wörtmann and Mohr's formula suggested by Sonderegger[36] is:

$$S_p = F'\left[K\frac{H}{S} + A(700 - T)\right]$$

where F' = breadth of weld at face, mm.
H = thickness of the weld, mm.
S = thickness of plate, mm.
K = a constant $= \frac{A \times 10^5}{C}$
A = 0.00001515 = coefficient of thermal expansion of iron, mm./mm.
C = 0.159 = average specific heat of iron
T = initial temperature of the plate, deg. C.

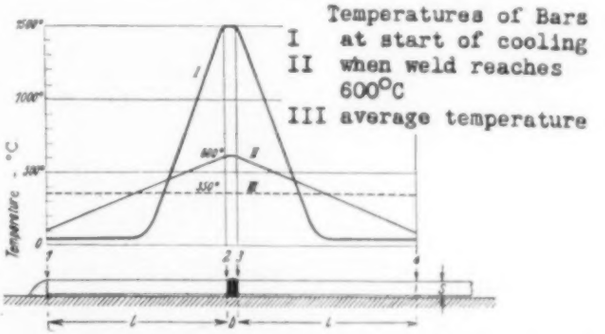

Fig. 10—Assumptions Made by Malisius[1] in His Theory of Shrinkage Across a Butt Joint

The theory of shrinkage proposed by Malisius[1] is based fundamentally on the conditions shown in Fig. 10. Two iron bars, of which the right-hand bar is free to move, are instantaneously welded together by the weld of breadth "*b*." Heat transfer by radiation is neglected, the temperature distribution being linear. It is assumed that the movements of the two bars due to temperature change are completely absorbed by the weld metal and are not transmitted from the left to the right until the weld has cooled to a temperature of 600° C., curve I. It is also assumed that the equalization of heat and temperature occurs in a length L = 12 cm. ($4^3/_4$ inches) on either side of the weld, the equalization temperature being 350° C.

On the basis of thermal expansion it is shown that on heating the two bars from 15 to 350° C. the distance between points 2 and 3 is narrowed by 1.06 mm. (0.0417 inch). On cooling from 350 to 15° C. after welding the left hand bar returns to its original position, but the right hand bar is drawn to the left a distance of 1.06 mm. To this distance is added the shrinkage of the weld metal itself from 600 to 15° C., which is 0.04 mm. (0.0016 inch) for a weld 0.20 inch wide.

These considerations are expressed in Malisius' formula:

$$S = \lambda_1 K \frac{Q}{S_1} + \lambda_2 b$$

where S = axial shrinkage perpendicular to the weld, mm.

λ_1 = linear thermal expansion of the bar from T_0 to $\frac{T_1 - T_0}{2} = 0.0044$

T_0 = initial temperature of the bar,

T_1 = temperature above which the material is no longer elastic ($T_1 > T_0$),

λ_2 = linear thermal expansion of the weld from T_0 to T_1, = 0.0093

Q = cross section of weld including reinforcement, sq. mm.

S_1 = average thickness of bars, mm.

b = average breadth of weld, mm.

K = a constant depending on the thermal output of the welding process and the thermal conductivity

K = 43 for arc welding, bare electrodes (S = 0.039 in.)

K = 45 to 55 for coated electrodes (S = 0.054 in. average)

K = 64 for atomic hydrogen welding (S = 0.056 in.)

K = 75 for oxyacetylene welding (S = 0.066 in.).

The values in parentheses are shrinkage values in inches if $S_1 = b = 0.20$ inch; $Q = 0.04$ sq. in. The factor K is determined by experiment.

In extending the formula to butt welds in plates, Malisius applies the purely arbitrary factor 0.6 to K in order to account for the decrease in heat supply due to the moving electrode, and multiplies S by the factor 1.3 to take account of the closing effect on the still unwelded gap exerted by the section already welded. The formula applied to butt welds is thus:

$$S = 1.3\left(0.6\lambda_1 K \frac{Q}{S_1} + \lambda_2 b\right)$$

Obviously the formulas of Malisius, and Wörtmann and Mohr are identical in form, the total shrinkage across a weld being composed of a part due to shrinkage of the weld and a part due to shrinkage of the plate. Neither formula is much better than empirical but, using the constants given, surprisingly close approximations to actual shrinkages perpendicular to a weld are obtained.

Calculation of the shrinkage distortion parallel to the weld has been attempted by Gerbeaux[60] with the aid of a constant to be determined experimentally. The constant, the numerical value of which is not given, is the longitudinal shrinkage of a bead of weld metal cut from its surroundings while still red hot. Given the constant, the derivation of formulas for the shrinkage of structural sections, such as plate girders, follows from elementary Strength of Materials. The remarkably low value of the shrinkage observed parallel to a weld is discussed by Schmuckler,[61] who, however, offers no general formula.

Practical Methods for Avoiding Welding Distortion

Much has been written on the subject of avoiding distortion during welding. As G. W. Plinke (private communication June 1937) pointed out, allowance deductions for shrinkage of welded pressure vessels and tanks are based on experience. He has found that peening is valuable in offsetting distortion of flat head welded circumferentially to a shell and of angles welded to plates. The usefulness of peening is by no means universally acknowledged. Piette[62] has written a book on methods applicable to oxyacetylene welding. Meller,[63] Dorrat,[35] Bierett[64] and Davidson,[65] provide good general reviews of basic principles. As shown in preceding sections, the effect of welding heat on base metal provides the major source of shrinkage. Tacking reduces shrinkage perpendicular to the weld and tends to provide a uniform distribution of shrinkage over the length of the weld. Step-back welding may also be of value (Erber[66]) in obtaining uniform welding. Angular distortion in multi-layer welds may be offset by reverse runs or by depositing layers alternately on either side of a symmetrical X joint. An increase in spacing between plates, as well as an increase in plate thickness or joint angle increase the shrinkage perpendicular to butt welds, but plate thickness has little effect on distortion in fillet welds.

Practical discussions of minimizing welding distortion in shipbuilding are given by Sherwin,[67] Osono,[68] Fukuda[69] and the Director of Naval Construction of the British Admiralty.[25] Hunter[70] had little trouble with distortion in shipwelding with material over $^3/_8$ inch thick, and Gerritsen[71] recommends the use of unbeveled plates for shipwelding up to $^3/_8$ inch thick. Höhn's book on Boiler Welding[19] discusses the practical aspects of distortion. A good method for compensating for distortion in welding the longitudinal seams of pressure vessels is illustrated by Philips.[72] Methods for patching boilers to avoid cracks are discussed by Kohrs.[73] Welding sequences for a complex pier member and a large drum are described by Boyd and Cape,[74] and similar details for a welded building are given by Electric World.[75] Hawley and Merrell[76] outline the procedure for minimizing distortion in welded bridge floors. Means of counteracting distortion in welding sheet metal are described by Blaha.[77] Hook[78] reviews methods useful for copper welding, and Ribeaucourt[79] for arc-welding aluminum and copper. Wilson[80] shows how distortion due to welding was taken advantage of to provide the desired arch in a road-grading tool. The welding of plate girders while they are under stress in a hydraulic press is a method advocated by Langford[81] to offset distortion. Developments in joint clamps and adjusters for avoiding transverse bend shrinkage are discussed by Eskilson.[11]

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Welding Aluminum and Its Alloys

An Extended Abstract of Recent German Literature[1]

IN attempting to secure authoritative as well as quantitative data on the strength of welded light metals, the German Welding Research Committee has reviewed the literature of the last few years. Of 150 articles, the Committee has selected fourteen[2] for their tabulation, which is not yet complete.

Fusion Welding Aluminum

On the basis of radiographic and metallographic examination, physical properties and corrosion tests, Buchholz concluded that the best filler metal for 1-inch thick commercial aluminum (99.5% purity) is aluminum itself with or without 0.2% Ti. Filler rods containing 5% Si are not acceptable.

The oxyacetylene welded specimens were made by the two-torch vertical method (X joint). The filler rods were commercial aluminum, aluminum containing 0.2% titanium and aluminum containing 5% silicon. The arc-welded specimens were single V, made in a horizontal position with commercial aluminum electrodes. The root layer was made with 0.32-inch electrodes, the three remaining layers with 0.39-inch electrodes, 270 amps, D.C. The plates were preheated and reverse welded. Each layer was chipped.

X-Ray Examination.—Radiographs showed very little flux entrapped in the gas-welded specimens. The arc welds tended to show more slag inclusions which required skill on the welder's part to avoid. The inclusions had little effect on bend ductility and tensile strength, but they seriously lowered the corrosion resistance. Traces of flux in the weld also has a bad effect on corrosion resistance.

Macroscopic Examination.—The structure of the gas welds in 1-inch plate made by the two-torch vertical method was much finer than that of single-V welds made

[1] Proceedings of the Welding Research Committee of the German Society of Engineers (VDI) No. 30, May 1937, page 6. Translated by Dr. G. E. Claussen, Research Assistant, Welding Research Committee.

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in one or two layers. The heat-affected zone generally had a coarse structure. Cold or hot peening was not successful in refining the structure of two-torch vertical welds, nor was hammering out the scarves of the X before welding, because the necessary deformation could not be attained throughout the 1-inch thickness. Rods containing titanium and silicon are the only means of securing fine-grained welds. Arc welds made in one layer had a coarser grain structure than gas welds. The effects of cold peening extended only a fraction of an inch below the surface. Multi-layer welding was of course effective in restricting coarse crystallization to some extent.

Tensile Properties.—Tensile specimens were 1.00 x 1.30 inches; the weld was machined flush. The modulus of elasticity of the weld was the same as base metal, but the yield strength and elongation of the welds were lower, as shown in Table 1. If there were no defects, fracture always occurred in the coarse crystalline, heat-affected zone 2 to $2^1/_2$ inches from the middle of the weld. The coarse grain in this zone is due to recrystallization, the cold work required for which is derived from cold straightening after annealing. The percentage elongation measured over the weld and its immediate vicinity was only 12 to 14%. The yield strength measured by means of an extensometer with gage length (2 inches) in the weld zone, as shown in Table 1, was about 30% lower than the yield strength determined by 0.2% offset in a length of 13 inches. The tensile strength determined on reduced section tensile specimens (no details) was 13,100 psi for base metal, and 12,100 to 12,800 psi for X or V joints prepared by any method. It was not found possible to correlate Brinell hardness with tensile strength.

Table 1—Tensile Properties of Aluminum Welds. Buchholz

Specimen	Yield Strength (0.2% Offset in 2 Inches Across Weld), psi	Tensile Strength, psi	Elongation, % in 13 Inches
Base metal	5100	12,000	34
Oxyacetylene weld, commercial Al rod, untreated	2500	11,300	24
Oxyacetylene weld, commercial Al rod, cold peened	3400	11,500	21
Oxyacetylene weld, commercial Al rod, hot peened	2750	11,300	20
Oxyacetylene weld, rod with 0.2% Ti, untreated	1900	11,300	23
Oxyacetylene weld, rod with 5% Si, untreated	2400	11,100	19
Arc weld, commercial Al electrode, untreated	2400	11,000	18
Arc weld, commercial Al electrode, cold peened	3400	Not given	Not given

Roller-Bend Tests.—All specimens developed 180° bend angle if no defects were present. The plunger diameter was twice the plate thickness. Oxyacetylene and arc welds made with commercial aluminum and oxyacetylene welds made with 0.2% titanium rod gave bend elongation in the weld of up to 75%, with or without peening. Since the welds made with the 5% silicon rods were harder than base metal, maximum bend elongation occurred within the heat-affected zone.

Corrosion Test.—In a stirred solution containing 3% NaCl and 0.1% H_2O_2 all the welds with the exception of the 5% silicon, had practically the same corrosion resistance as base metal. After 63 days the 5% Si welds were heavily corroded. The welds made with 0.2% titanium rods had slightly lower corrosion resistance than base metal (no details).

Spot and Seam Welding Aluminum.—The chief factor contributing to the successful spot and seam welding of aluminum and its less heat-sensitive alloys, such as Hydronalium (10% Mg, 90% Al) is the Thyratron or similar controller to deal accurately with the high power input (up to 400 kva.). This is especially true, as Schober points out, of seam welding because in aluminum and its alloys seam welds must consist of a series of more or less overlapping spots (10 spots a second, for example); continuous current cannot be used. The accuracy of the usual tube controller is $^1/_{1000}$ second. The pressure on the electrode rollers should be adjustable between 175 and 900 lb.

Accurate timing as well as pressure and velocity control are necessary for successful welds. Given well balanced conditions, however, spot and seam welds without any trace of cracks, blow-holes, or shrinkage cavities can be produced even in such difficult alloys as Hydronalium. Schober recommends the use of Alclad for spot-or seam-welded Duralumin that must withstand corrosive conditions. The Dural weld in Alclad is entirely enclosed in pure aluminum.

Oxyacetylene Welding Heat Treatable Alloys.—The alloy used by Maier was K. S. Seewasser (1.3 Mn, 2.2 Mg, 0.2 Sb, 0.7 Si, rem. Al, 0.16 inch thick) in three heat treatments, shown in Table 2. Single V oxyacetylene welds were made in plates of each grade, 10 x 20 inches, using a weak torch, strips of base metal as filler rod, and Autogal D as flux. Specimens were tested cold peened, and hot peened by hand, as well as untreated. Hot peening was at a temperature at which sawdust chars. Tensile, face bend and hardness tests were made, any reinforcement being machined flush.

Table 2—Grades of K. S. Seewasser Used by Maier

Grade	Tensile Strength, psi	Elongation, % in $11.3\sqrt{A}$	Bend Angle, Degrees	Brinell Hardness
Soft	30,500 to 31,000	13–15	180	55
Half hard	35,000 to 37,000	4–5	180	65
Hard	40,000 to 40,500	3–4	30–60	75

Of 90 tensile specimens, only 14 broke in the weld on account of defects. The remainder fractured at a distance of $^5/_8$ to 2 inches from the center of the weld. The average tensile strength of those specimens was 28,600 to 29,400 psi, regardless of grade and treatments. All specimens withstood 180° bend. The hardness of the cold-peened welds was 85 Brinell; the hardness of the other welds was 60 Brinell.

Maier also found that cold hammering the heat-softened zone did not increase the unit strength. To clinch the argument he machined a number of welds flush and reduced weld and plate $2^1/_2$, 5 or 10% by cold rolling. The maximum average tensile strength of any group of these specimens was 32,500 psi, which is only slightly higher than that of soft plate. All specimens failed in the bend test at 30 to 90°. The hardness of the welds varied from 65 to 90 Brinell, depending on treatment. The low-bend dutility is explained by the hardness of the cold-rolled heat-affected zone, which forced failure in the weld.

It seems plain from Maier's work that the complete solution to the problem of welding heat-treatable aluminum alloys has not yet been obtained. Mechanical treatments of all sorts provided less improvement in the strength of welds in such alloys than might have been expected.

THE ENGINEERING FOUNDATION
WELDING RESEARCH COMMITTEE

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Corrosion Resistance of Welded Joints
A Review of the Literature to January 1, 1937

By W. SPRARAGEN* AND G. E. CLAUSSEN**

This Report Is Prepared Under the Auspices of the Literature Division of The Engineering Foundation Welding Research Committee

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Caution.—Throughout the review, including the Summary, the conclusions arrived at are those of the investigators, not of the reviewers. Corrosion research on welds whether in service or in the laboratory is beset by a multitude of variables that are proverbially uncontrollable. For this reason a thorough appreciation of the testing conditions is of paramount importance in the interpretation of the results of any investigator.

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SUMMARY

Corrosion Resistance of Welds in Mild Steel

General Remarks.—The corrosion of welded joints is dependent on a number of factors, such as corrosive media, temperature, method of exposure, contact with other substances, as well as the quality of the welded joint itself.

Mild Steel.—Good welds made in mild steel by the arc (bare and covered electrodes) and gas processes are not subject to excessive corrosion as compared with the base metal in tap water, boiler feed water, gasoline and sea water, weather, sea water, salt water, sea water spray. On the other hand, there is some slight preferential corrosion of bare wire (but not covered wire or gas) welds in: 15 to 20% H_2SO_4, sea water and air, 1% H_2SO_4 and air, boiling 10% HCl.

There are also data on covered electrode welds (but not on bare wire or gas) which would indicate that these welds have nearly as good corrosion resistance as base metal in sea air, 26% NH_3 at 100° F., boiling 10% acetic acid and boiling 85% H_2SO_4.

There is considerably greater corrosion in gas and arc (bare and covered) welds than in the mild steel in combinations of hot air and water, alternate immersion in salt solution and HCl.

In 15 to 20% H_2SO_4 small amounts of Cu and Cr in plate and rod help resist corrosion, whereas sulphur and phosphorus are bad. In alternate sea water and air small amounts of Ni are helpful, and S and P are adverse.

Electrochemical Potential of Arc Welds.—Potential measurements are sometimes in agreement with, sometimes exactly opposite to, the corrosion test results.

Corrosion of Arc Welds in Service.—The behavior of arc welds in ships has been excellent. Excessive corrosion does not occur in welded piping and hangers in oil tanks or elsewhere on ships. Corrosion results from high-temperature, high-pressure vessels handling very corrosive crude and gas oils show consistently that weld metal is no more subject to corrosion than base metal. Poor service corrosion behavior of metal arc (bare electrodes) welded wrought iron pipe joints in technical H_2SO_4 as well as in ammoniacal steam containing small amounts of sulphurous acid has been reported.

Corrosion Tests of Gas Welds.—Tests on 6-in. pipe joints oxyacetylene welded with the three most typical combinations of rod and welding technique including exposure for one year to atmosphere, soil immersion, tap water and atmosphere, and salt spray, as well as short time tests in HCl and short time electrolysis tests in salt water show no appreciable difference in corrosion resistance among the welds or between the welded and unwelded pipe. Oxyacetylene welds in boiler plate have withstood caustic embrittlement conditions in highly concentrated lye.

Corrosion Resistance of Carbon-Arc Welds.—Carbon arc welds in mild steel made in a shielding gas have better general corrosion resistance than unshielded.

Corrosion Resistance of Forge Welds.—Tests of good forge welds indicate that the weld has the same corrosion resistance to caustic soda, 5% solution HCl and H_2SO_4, and 20% HCl as base metal.

Corrosion Resistance of Electric Resistance Welds.—Flash welds show good corrosion resistance to tap water, as do also spot welds in freight car service.

General Service Corrosion Results.—In service, welds have given excellent account of themselves in pipe lines, oil pipes, ship work and submarine structures.

On the other hand, poor results are reported in welded wrought iron drain pipe, and conveyors in a beet sugar factory.

Caustic Embrittlement.—Caustic embrittlement is not generally encountered in good welding. Exceptions have been noted under severe conditions. Stress relieving has helped in these situations.

Effect of Differences in Composition.—Even large differences in composition between base metal and weld metal may not lead to local effects, as for example, Krupp austenitic welds in mild steel.

Mild Steel Welds versus Rivets in Corrosion.—Whatever difference there may be between the corrosion resistance of riveted and welded joints under corrosive conditions appears to be in favor of the welded joint.

Corrosion of Welds in Low-Alloy Steel

Surprisingly little information on the corrosion of welds in low-alloy steels is available.

It has been stated that in low-alloy steels with reinforced welds made with covered electrodes, the life of the weld in corrosion is as long as base metal.

Corrosion of Welds in Stainless Ferrous Alloys

Spot and acetylene welds made in stabilized 18–8 stainless are definitely superior to unstabilized steels in their corrosion resistance to salt spray. Unheat-treated gas welds in 18–8 when subjected to boiling H_2SO_4-$CuSO_4$ solution are not attacked if the carbon content is low (approximately 0.05% or less), but are completely destroyed in a day and a half when the carbon content is 0.14% or over, unless "stabilized." Arc welds in 18–8 resist attack by, although not designed for use in, sea water, sulphite, 10% HCl or 10% H_2SO_4 or HNO_3 but are corroded in acid $CuSO_4$ unless the carbon is low or the steels stabilized. Heat treatment improves the resistance of arc welds in 18–8 when subjected to 50% HCl. Stress relief (1 hr. at 1200° F. and air cooled) lowers resistance of welded joints to corrosion by HNO_3 even if carbon is as low as 0.06 C.

Proper heat treatment after gas welding protects 18–8 (plain and alloyed, 0.13 to 0.20 C.) from boiling 10% $CuSO_4$ and 10% H_2SO_4. In unheat-treated welds titanium, columbium (sometimes known as niobium), and vanadium offer protection, but heat treatment is required for maximum corrosion resistance.

Spot welds in 18–8 if made in less than 0.1 sec. resist 20% NaCl.

Metallurgy.—The metallurgical aspects of the corrosion of welded 18–8 center around the intergranular precipitation of carbides in the heat affected zone of welds. The intergranular carbides produce the effect known as intergranular corrosion. The presence and magnitude of the effect depends on the composition and heat treatment of the steel, the process of welding, and the corrosive agent involved. A basis for weldability of 18–8 is stated to be the length of time in the temperature range 600 to 700° C. required to produce intergranular corrosion in boiling H_2SO_4-$CuSO_4$ solution or boiling 50% or, more commonly, 65% HNO_3.

Stabilizing elements, such as Cb, Ti and Si may act in two ways to prevent intergranular corrosion. Carbide-forming elements prevent local dilution of the sensitized region in chromium. Ferrite-forming elements restrict carbide precipitation to the ferrite islands; the grain boundaries are not sensitized. A decrease of carbon content is effective in reducing intergranular corrosion by reducing the amount of carbide available for precipitation. Shrinkage stresses may hasten intergranular corrosion.

General Observations—Carbon Content.—The effect of increasing carbon content is to increase the tendency

to intergranular corrosion. The carbon content of base metal and filler rod should be below 0.07%.

Stabilizing Elements.—Suitable stabilizing elements are Ti = 6 × C or Cb = 10 C; other stabilizing elements reported are (0.6 Ti) (2 Si) (1.9 W). The efficacy of silicon and tungsten is doubted by some authorities.

Heat Treatment.—As indicated in the section on experimental results, correct heat treatment raises the corrosion resistance of welds in unstabilized 18–8, and to a very minor extent, in stabilized 18–8. The heat treating temperature for developing maximum corrosion resistance in welded 18–8 is 1900–2100° F. (1040 to 1150° C.), depending on carbon content.

Technique.—It is essential that welds in 18–8 be given a good finish in order to develop good corrosion resistance.

Plain Chromium Stainless Alloys.—The corrosion resistance of properly heat-treated 4–6 Cr weld metal is the same as base metal. This is also true of 12–16 Cr alloys. In general it is advisable to have the carbon below 0.14%.

Corrosion of Welds in Aluminum and Its Alloys

Experimental Corrosion Tests on Welded Aluminum.—Spot welds in 2S and 3S resist salt spray. Gas welds in aluminum resist many acids (except HCl and H_2SO_4), dye agents, fuel oil, acetone, salt water, sea water and tap water. This is also true of metal and carbon arc welds.

The use of 5% Si in rods for gas welding thick aluminum plates results in welds inferior in resisting corrosion attack in 3% NaCl, 0.1% H_2O_2, as compared with welds made with commercial aluminum rods. On the other hand, there seems to be no difference in welds produced with these wires in resisting salt spray and tap water.

In general as with Al, spot-welded alloys suffered no significant losses after exposure to salt spray for one year.

General Observation on Aluminum Alloy Welds.—Where the properties of the alloys are not dependent on heat treatment the corrosion resistance is generally at least as good as welds made in aluminum.

Corrosion of Welds in Copper and Its Alloys

Copper.—Gas welds in electrolytic copper using flux and special rods resist corrosion in acetic acid to the same extent as the base metal. Annealing at 600 to 700° C. has no effect; higher annealing temperatures increased the rate of attack. In ordinary welds, however, the rate of corrosion in acetic acid is much greater.

Copper Alloys.—Gas welds in special bronze (50 Cu, 38 Zn, 10 Ni, 2 Mn) made with flux, pickled and neutralized in 1% HNO_3 after welding have better corrosion resistance than the base metal in many dye chemicals, acids, fuel oils, lubricating oils and acetone. Annealing is of no advantage. Gas welded inserts in brass condenser tubes require no heat treatment to develop good corrosion resistance.

Corrosion of Welds in Nickel and Its Alloys

Nickel.—Welding rod for nickel and its alloys should have the same composition as base metal to avoid electrochemical action. Welded nickel dairy equipment is perfectly reliable. Nickel under stress, such as that caused by arc welding, may be sensitive to acetic acid corrosion.

Inconel.—Inconel welds of good quality show no attack after long periods in acid refrigerating brines and are at least as corrosion resistant as unwelded sheet. A factor contributing to the good corrosion resistance of welds is that Inconel is relatively free from disturbances due to strain and recrystallization. After 100 hours in acid $CuSO_4$ Inconel welds developed no intergranular corrosion.

Monel Metal.—Monel Metal welds are not resistant to a $CuSO_4$-acetic acid pickle, and are of doubtful value in hypochlorite liquor and concentrated formic acid. They are entirely resistant to dilute sulphuric acid, hot and cold soap solutions, dilute formic acid, cold sodium sulphide solutions, and many technical dye reagents.

Corrosion of Welds in Other Non-Ferrous Metals

Magnesium.—Pickled gas welds in electron are superior in general corrosion resistance to base metal, on account of vaporization of zinc. The seam is first washed in water, then pickled in HCl-chromate bath which forms a bronze-colored corrosion-resistant coating. In castings, the fine grain of the weld metal also promotes corrosion resistance.

Zinc.—Corrosion resistance of gas welds in 99.99% zinc is good.

Lead.—Service corrosion results on welded lead, soft or antimonial, are as satisfactory as on unwelded lead.

Corrosion of Welds in Galvanized Steel

Investigations show that, in many cases, regalvanizing or painting the welds is not necessary. Welds made with bare and coated electrodes showed rust stains to about the same extent. Multi-layer fillet-welded specimens, however, showed a considerable amount of rust after 1100 hrs. in salt spray. Bronze-welded specimens of galvanized sheet, prepared in several ways and also coated with aluminum paint after galvanizing, always showed less attack in salt spray tests than fusion-welded specimens. A bronze-welded joint in galvanized steel was not affected by immersion in $CaCl_2$, or by weathering. Spot- and flash-welded galvanized sheets resist weathering, and spot welds also resist salt water corrosion. Service corrosion results of bronze-welded galvanized iron acetylene generators have always been satisfactory. Much has been written concerning methods of welding galvanized material, but the center of discussion is usually the brittleness (iron-zinc compound) rather than the corrosion resistance of the joint.

Corrosion of Bronze-Welded Joints

Bronze-Welded Cast Iron.—Electrolytic action or preferential corrosion of bronze or cast iron is not usually observed. Cast-iron bronze-welded specimens resist corrosion in sea water, and the most corrosive soils; on the other hand, copper-nickel or mild steel electrode welds in cast iron are not satisfactory in some corrosive conditions.

Corrosion of Soldered Joints

Soft soldered joints in aluminum may fall apart after a few months in sodium chloride solution.

Aluminum soldered with zinc has poor corrosion resistance. The corrosion resistance of aluminum soldered with cadmium is improved by heat treating the joint.

Hard solders (Al base) exceed all soft solders for Al in corrosion resistance. The corrosion resistance of joints made with a hard solder containing 11.5% Si, rem. Al may be particularly good.

Lap joints in 18–8, 18 Cr-2Ni, 13 Cr (pickled anodically except for 18–8 which was used in the bright-rolled condition) made with three solders: (1) 64–66 Sn, 1.0 Sb, rem. Pb; (2) 49–51 Sn, 2.4–3.0 Sb; rem. Pb; (3) pure Sn, and two fluxes: orthophosphoric acid and $ZnCl_2$, were exposed in a laboratory, in the open air, and to a

sea-water spray test for 6 to 14 months. If the joints had been thoroughly washed, no corrosive effects were observed. Residues of flux, however, caused corrosion which was much more serious with $ZnCl_2$ than with orthophosphoric acid.

Soft soldered joints in unstabilized and stabilized 18–8 were subjected to 20% NaCl spray for 500 hr. at 35° C. Soft soldering did not cause intergranular corrosion in either type of 18–8.

The Navy uses lead-tin solders for all applications involving fresh water or sea water. Soldering or riveting is not satisfactory for stainless steel restaurant equipment, on account of the corrosion problem. Under some conditions, soldering seriously lowers the corrosion resistance of cupro-nickel alloys. The corrosion resistance of aluminum soldered to brass is bad.

Up to about 3.5% Sb has no effect on the appearance and corrosion resistance of soldered joints in steel. With higher Sb content, moist air may cause deterioration. Arsenic is harmful to corrosion resistance. Up to 3% Zn or Cu is also harmful. Over 0.05% Zn gives rise to oxided films, which reduce the corrosion resistance. Zinc and aluminum as well may cause corrosion of electrical connections, if either is present in the solder.

Silver Soldered Joints.—Silver solders are resistant to ordinary atmospheric corrosion, but are attacked by HNO_3 and are discolored by gases or products containing sulphur. Where Zn is harmful to corrosion resistance the Ag-Cu eutectic solder is employed. Overheating damages the corrosion resistance. Silver solders are resistant to many of the reagents to which high-nickel alloys are resistant.

The Effect of Flux on the Corrosion Resistance on Soldered Joints.—Corrosive soldering fluxes for copper and other metals are sources of failure. Fluxes containing chlorides accelerate the corrosion of all soldered joints, particularly those containing zinc, or those containing blow-holes or other crevices. A cyanide or alcohol wash or hot water dip is recommended to remove flux (NH_4Cl plus $ZnCl_2$) from soldered seams on copper and galvanized iron.

Corrosion Fatigue

The corrosion fatigue limit of welded boiler steel is reduced 25% in fresh water according to some tests made by the U. S. Navy.

The Corrosion Resistance of Flame-Cut Surfaces

Flame cut surfaces in mild steel have better corrosion resistance than planed surfaces.

Corrosion of Welds at Elevated Temperatures

The resistance of welds to scaling or high-temperature oxidation seems to be the same as base metal, provided the weld has approximately the same composition as base metal.

The attack of welded joints by hot, high-pressure hydrogen in hydrogenation apparatus is more pronounced the more the weld metal differs in composition from base metal.

Corrosion Resistance of Welded Joints

Corrosion Resistance of Welds in Mild Steel

The following summary of investigations into the corrosion resistance of welds in unalloyed mild steel is intended not only to state all results that have been obtained, but also to make clear the different kinds of testing procedures that have been employed. In summarizing each investigation, the reviewers have attempted to give all experimental details, so far as they are known, in order that the results may be interpreted with as little ambiguity as possible. To facilitate references, the investigations have been arranged according to metal, process of welding, and chronologically. The sections on welding processes are followed by sections on the technical problems of corrosion particularly affecting welded joints.

In the words of Texter and Speller:[1] there is no material difference between weld metal and base metal in corrosion unless the former contains fissures or discontinuities, or has a radically different electrolytic potential, or if the magnetic oxide scale on base metal differs from that on weld metal, giving rise to electrolytic action.

Combined Investigations of Gas and Arc Welds

1936—Hatfield[2]

Pickled, gas- and arc-welded specimens of mild steel in the unmachined condition were totally immersed for 4 weeks in a boiling solution containing $CaSO_4$, $MgSO_4$ and NaCl, corresponding to boiler operation. The specimens were 3 x $^3/_4$ x $^1/_2$ inch thick and the loss in weight was expressed as grams, not related to area of specimen. The electrodes were: 1. bare, 2. lightly fluxed, 3. iron silicate coating, 4. blue asbestos coating. The carbon content was between 0.08 and 0.15%. Although the results were not particularly consistent, the welded specimens appeared to suffer a slightly greater loss than unwelded, which may perhaps be explained by the greater exposed surface area of the welded specimen. There was no indication of preferential attack in the junction zones or of pitting in the welds. There was no definite difference between the electrodes, but the gas welded specimens were slightly less corroded, on the whole, than the arc welded. Annealing or normalizing the plate prior to welding appeared to have no effect on corrosion resistance of the welded joint.

1936—Brillié, Leroy and Roux[3]

These investigators made probably the most elaborate corrosion tests of welds that has ever been made. The base metal and filler rods tested are listed in Table 1. The values for the filler metal refer to all-weld-metal specimens. The electrodes were 0.16 inch diam.; the coating was 0.028 inch thick, except on No. 2, the coating of which was 0.020 inch thick. The electrodes were connected to the negative pole. The oxyacetylene filler rods were 0.20 in. diam. and welding was done at the rate of 3.3 feet per hour, using 99% pure oxygen and 35 cu. ft. acetylene per hour. Butt welds (70° V) were made in two plates each $6^1/_2$ x $2^1/_2$ inches thick. Three specimens $4^1/_4$ inches. long (perpendicular to weld) and $1^1/_2$ inches wide were cut from each weld, which was not heat treated in any way. All specimens were either milled, milled and sand blasted or the weld ground flush.

Continuous Immersion Tests were carried out in distilled water containing 20 % H_2SO_4 by weight, at 30° C. for 48 hr. The specimens lay obliquely inclined in uncovered beakers containing 750 ml. acid. Corrosion

Table 1—Materials Tested by Brillié, Leroy and Roux[3]

Designation	C	Si	Mn	Cr	Cu	Ni	S	P	Tensile Strength psi	Elastic Limit psi	Elong. %
Base Metal X	0.05	nil	0.27	..	..	..	0.051	0.013	49,200	31,400	53.3
Base Metal S	0.33	0.02	0.54	..	..	..	0.024	0.019	72,000	40,700	43.3
Base Metal A	0.13	nil	0.49	..	0.40	..	0.040	0.016	63,000	40,500	30
Base Metal D	0.20	0.27	0.70	0.52	0.57	..	0.031	0.026	76,000	47,000	40
Electrode 1	0.11	0.01	0.01	..	..	..	...	...	56,500	41,700	18.5
Electrode 2	0.05	0.19	0.75	..	..	..	...	...	68,000	52,800	25.7
Electrode 3	0.19	0.04	0.20	0.19	0.58	..	...	...	67,500	56,600	20
Electrode 4	0.02	0.02	0.23	..	..	1.60	...	...	58,000	46,200	20
Gas Rod 5	0.04	0.03	0.41	..	..	..	...	...	53,300	41,200	23
Gas Rod 6	0.10	0.05	0.35	0.34	0.66	..	...	...	66,500	49,100	23
Gas Rod 7	0.14	0.20	0.49	..	..	..	...	...	68,500	48,500	16
Gas Rod 8	0.13	0.11	0.41	..	..	3.25	...	...	84,500	58,000	12

(Electrode 1—Acid coating; 120 amps.) (Gas rod 5—Gassy pool)
(Electrode 2—Semi-volatile coating; 110 amps.) (Gas rod 6—Gassy pool)
(Electrode 3—Acid coating; 140 amps.) (Gas rod 7—Quiet pool)
(Electrode 4—Acid coating; 125 amps.) (Gas rod 8—Quiet pool)

was estimated by appearance and by decrease in dimensions. Letting a = change of thickness of plate, b = change of thickness of weld, $a - b$ = relative corrosion of weld, a negative value signifying preferential corrosion of the weld. Loss of weight measurements did not offer clear indications of behavior in corrosion unless weld metal was very much more, or less, attacked than base metal. Measurements of loss of weight were made, but no significance was attached to them. The change of dimensions of milled and sand-blasted specimens is shown in Table 2.

Table 2—Continuous Acid Immersion Tests of Butt Welds. Brillié, Leroy and Roux[3]

Change of Dimension (a-b), In.

	Filler Rod							
Base Metal	1	2	3	4	5	6	7	8
X	Weld metal heavily attacked	−0.096	+0.008	Not det'd	Weld metal heavily attacked	+0.006	+0.004	+0.006
S	Weld metal heavily attacked	−0.033	+0.049	+0.037	−0.10	+0.049	+0.043	+0.045
A	Weld metal heavily attacked	−0.012	−0.002	−0.043	Weld metal heavily attacked	+0.002	0	+0.004
D	Weld metal heavily attacked	−0.008	−0.047	−0.012	Weld metal heavily attacked	−0.004	−0.002	+0.004

Irrespective of base metal, filler rods 1 and 5 gave very rapid attack due to blow-holes or oxide inclusions whereas the others, which produced clean, deoxidized welds, or contained Cr and Cu, were very resistant. In no case was the base metal in junction or other zones preferentially attacked or protected. It seemed, however, especially in the gas welds, that absorption of Cu and Cr from base metal by weld, or vice versa, afforded protection to the weld or base metal concerned. Chemical analyses were not made. The surface finish of the specimens had little effect on corrosion in the Cu and Cr-Cu steels, but with plain-carbon base metal, fine-machined specimens suffered 5 to 15% less corrosion than specimens with weld ground flush and sand blasted.

Alternate Saline Immersion Tests were carried out in 10 liters of distilled water containing 300 gm. NaCl, 1.876 gm. disodium phosphate and 12.404 gm. boric acid, which corresponds to artificial sea water used by French Aeronautics Bureau. The specimens were dipped 15 min. in 900 ml. of solution at 30° C., then withdrawn into the air for 15 minutes, the test continuing for 6 days (288 immersions). The specimens were suspended by unsized hemp string, which seems to have had an effect on the appearance of the specimens at least, which the investigators do not explain. Corrosion was estimated by the same methods employed in the continuous immersion tests. There was no change in the mechanical properties of the welds as a result of corrosion. The change of dimensions of milled specimens is shown in Table 3.

In contrast to the acid solution, most of the specimens corroded uniformly in the saline solution, and there was less difference between the filler rods. Although the results can scarcely be said to confirm the conclusion, Brillié and coworkers state that the order of corrodibility is about the same in saline and acid solutions. Further in agreement with the acid tests, there was no detectable effect of the weld metal on the behavior of the base metal in saline solution. In the early stages of the test the sand-blasted specimens corroded more rapidly than the milled. The reverse was true in the later stages.

Salt Spray Tests are still in progress. Preliminary

Table 3—Alternate Sea Water Immersion Tests of Butt Welds. Brillié, Leroy and Roux[3]

Change of Dimensions (a-b) In.

	Filler Rod							
Base Metal	1	2	3	4	5	6	7	8
X	−0.0040	−0.0024	+0.0004	Not determined	−0.0012	+0.0028	+0.0004	+0.0043
S	−0.0044	−0.0040	+0.0020	+0.0020	−0.0008	+0.0032	+0.0008	+0.0036
A	−0.0040	−0.0036	0	+0.0004	−0.0008	+0.0002	+0.0004	+0.0051
D	−0.0040	0	−0.0047	+0.0012	−0.0036	+0.0020	−0.0016	+0.0028

Table 4—Composition of Steels Tested by Diepschlag[4]

Material	C	Si	Mn	P	S	Cu	Cr
Armco Iron	0.013	..	0.017	0.005	0.025	..	...
Plain-carbon steel, st. 37, 53,000 psi min. tensile	0.08	0.15	0.58	0.06	0.05	..	...
Plain-carbon steel, st. 42,—60,000 psi min. tensile	0.12	0.17	0.69	0.04	0.05	..	...
Low-alloy structural steel 74,000 psi min. tensile	0.28	0.11	0.72	0.03	0.06	0.2	0.2
Gas rod	0.05	..	0.40	0.025	0.025	0.15	...
Bare electrode	0.10	..	0.55	0.025	0.025	0.15	...
Covered electrode	0.23	0.16	0.80	0.03	0.02	..	...

results show the same trend as the alternate saline immersion tests.

The investigators conclude that the same filler rod gives the same corrosion resistance for unalloyed steels and for Cu-Cr and Cu steels. The base metal must be free from inclusions, and should contain some alloy, such as Cr-Cu, Mn-Si, or Ni. Nickel gives best results in saline solution, whereas Cr and Cu give best results in acid. The base metal should be low in S and P.

1936—Diepschlag[4]

Gas and arc-welded specimens ($1^1/_2$ inch square and 0.20 inch thick) made with the materials listed in Table 4 were exposed for 8 weeks in a current of air saturated with water at 50 and 75° C.

The results given in Table 5 are taken from Diepschlag's graphs at 56 days and are expressed as loss of weight in milligrams/cm^2.

The experimental results, plotted as time *vs.* loss of weight, showed rather large unexplained irregularities which, it is felt, make detailed analysis of the results unwarranted. It is fairly clear that welding increases the rate of corrosion about 10% and that gas welds are slightly better than arc. Diepschlag's attempt to relate corrosion rate to composition of base metal and electrode has been shown to be unsuccessful by Lohmann.[5]

1927—Owens[6]

Gas and metal arc-welded specimens (V butt, all ground flush, some peened) in $^3/_8$-inch medium steel plate were immersed in conc. H_2SO_4 for 2 months. There was no appreciable corrosion.

Experimental Corrosion Tests of Metal Arc Welds

1937—German Marine[3,7,6]

Continuous immersion tests (50 hr. in 30% HCl at 18–20° C.) of mild steel (0.08 C., 0.5 Mn, 0.02 Si, 0.04 P, 0.03 S, 0.18 Cu) with a bead of weld metal on the surface were made using eight different mild steel electrodes. The compositions of the electrodes were nearly the same, the weld metals differing only in nitrogen content, 0.16% for bare, 0.04% for covered. Visual inspection of the specimens showed that the order of merit in corrosion was: covered, thin coated, cored, bare. The covered electrode specimens showed little attack; the cored and bare were severely attacked. V butt welds made in the same steel with covered electrodes had good corrosion resistance whether welded with D.C. or A.C.

Using disk specimens (see section on Corrosion Tests for Welds in Mild Steel), the German Marine found that mild steel deposited by covered electrodes had better corrosion resistance (20 hr. in 30% HCl) than rolled mild steel of the same composition (0.08 C, 0.5 Mn, 0.05 Si, 0.03 P, 0.03 S, 0.15 Cu).

As a general rule high notch impact value and good resistance to 30% HCl are considered to be the qualities of electrodes most important for corrosion resistance.

1936—Kusmak and Slomjanskaya[7]

V butt welds (covered and bare electrodes) were made in a steel containing 0.17 C, 0.56 Mn, 0.009 Si, 0.055 P, 0.061 S. Some specimens were normalized 30 min. at 900° C., air cooled. All specimens were completely resistant to NaOH (500° Bé.) at 90 to 100° C. In 5 N H_2SO_4, bare electrode welds corroded $2^1/_2$ to 3 times faster than covered, which is explained by the high nitrogen content of the former. Normalized welds were more resistant than as-welded specimens. Slag and gas inclusions accelerated corrosion.

1936—Schoenmaker and Blokker[8]

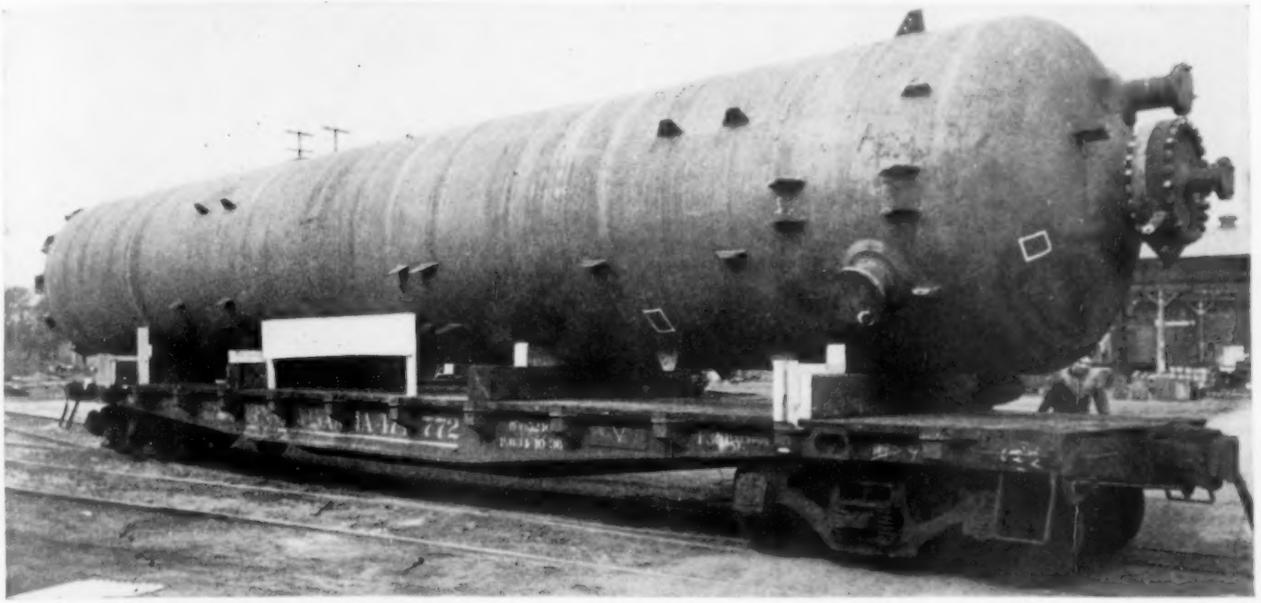
Table 6 shows the loss of weight of all-weld-metal cylinders (1.4 sq. in. surface area) in artificial sea water (3% NaCl plus 1% H_2O_2). The Resistens electrode was heavily coated, and the low-alloy structural steel contained Mn, Cr and Cu. In the absence of chemical analyses, little can be concluded except that the bare electrode is slightly inferior to the covered, and that the decrease in corrosion rate with time is about the same for unwelded plate as for all-weld-metal.

Table 6—Sea Water Corrosion of All-Weld-Metal. Schoenmaker and Blokker[8]

Specimen	Loss of Weight—Grams 100 Days	200 Days	300 Days	Tensile Strength, psi	Elong., %
Bare electrode	2.0	3.0	3.4	48,500	3.6
Resistens electrode	1.9	2.8	3.0	72,000	30
Unwelded plain carbon steel	1.95	2.9	3.25	53,000	..
Unwelded low-alloy structural steel	1.9	2.8	2.95	74,000	..

Table 5—Moist Air Corrosion of Welded Steel. Diepschlag[4]

Materials	50° C. Unwelded	Gas	Bare	Covered	75° C. Unwelded	Gas	Bare	Covered
Armco Iron	14.5	17	21	20	88	101	100	109
Plain-carbon steel 37	12.5	18	23	19	79	93	96	96
Plain-carbon steel 42	12.5	17	19	18	91	...	101	121
Low-alloy structural steel	10.5	18	19	9	105	108	118	...



Welded Pressure Vessel for Refinery Service. Of Carbon-Molybdenum Steel for Operation at 917° F., This Vessel Has 1 17/32-Inch Wall as Against 3 1/32-Inch Wall Required by Plain Carbon Steel. It Is Believed to Be the First of This Material Fabricated for This Service. The Babcock & Wilcox Company

1936—Poplavko[9]

Continuous immersion tests in 4, 8 and 12% HCl for periods of 1, 2 and 3 hours, were made on specimens of mild steel (0.08 to 0.15 C, 0.18 to 0.44 Mn, 0.02 Si) butt welded (single V) or with beads of metal deposited on the surface by metal arc process. The mild steel was 0.04 to 0.12 inch thick and the electrodes were about 0.05 to 0.10 in. diam. The current used for welding ranged from 10 to 40 amps. The results showed that reversed polarity produced welds with much lower corrosion rates than straight polarity, especially in 12% HCl. The results also showed that there was an optimum current (about 20 amps. for 0.05 in. diam. electrodes) that gave best corrosion behavior. It was remarkable that the concentration of acid had little effect on the optimum current, but the time of test (1, 2 or 3 hrs. in 8% HCl) had a large effect.

1936—Slutska[10]

The corrosion of welded and riveted joints was determined in a solution of 160 gm. NaOH plus 200 gm. NaCl in 1 liter of water at 95° C. The steel contained 0.09 C, 0.37 Mn, 0.19 Si, 0.03 P, 0.047 S, and was 0.43 inch thick. The samples were 4 x 3 5/8 inch, the joints being parallel to the shorter edge. The welds were made with 0.20 inch chalk-coated electrodes, 170 amps. and were 70° V. The riveted joint was single lap, 2 rivets, each 3/4 in. diameter. The corrosion rate was determined from the amount of hydrogen evolved. Approximately three times as much hydrogen was evolved from the riveted joint as from the welded.

Similar specimens were also partly immersed, the joint being perpendicular to the surface of the liquid. The water line effect was determined by subtracting from the actual amount of iron that went into solution, the amount that would have been dissolved from an equivalent specimen totally immersed. The water line effect was 25% more severe in the riveted than in the welded joint. Unjointed specimens were not tested.

1936—Svegintsev[401]

Mild steel plates (no analysis), 0.63 inch thick, were butt welded, 80° V, with coated electrodes, 0.16 inch diameter. The results of continuous immersion tests in acids at 15 to 20° C. for 96 hours are given in Table 7. The corrosion rate is expressed as grams per square meter per hour.

1935—Moses[11]

The following specimens were prepared from covered electrode welds stress relieved 1200° F. furnace cooled, in 1-inch plate: *A*. all-weld-metal, *B*. plate metal 2 1/2 to 3 inches away from the weld, *C*. plate metal at junction zone, *D*. cross section of weld including plate 1 1/2 inch on each side. The specimens were polished with No. 1 emery cloth, and suspended from glass hooks for 2 hrs.

Table 7—Corrosion Rates of Butt Welds in Acids. Svegintsev[401]

Type of Coating	Chemical Composition, %							10% H_2SO_4		10% HCl		10% HNO_3	
	C	Mn	Si	S	P	Ni	Cu	Rate	%	Rate	%	Rate	%
Chalk	0.11	0.20	Trace	0.014	0.021	..	..	8.76	100	13.73	100	95.12	100
"LIM" Covering	0.11	0.20	Trace	0.014	0.021	..	..	5.64	64.5	6.39	46.5	84.90	89.0
Chalk	0.10	0.31	Trace	0.03	0.022	0.09	0.21	5.60	64.0	11.83	86.2	60.77	64.0
Chalk	0.7	0.30	0.07	0.022	0.007	..	0.16	5.55	63.4	10.20	74.3	64.27	67.6
"LIM" Covering	0.7	0.30	0.07	0.022	0.007	..	0.16	2.31	26.4	3.63	26.4	79.50	83.6
Chalk	0.8	0.28	0.06	0.022	0.007	..	0.47	4.12	47.1	6.03	44.0	67.34	70.7

Table 8—Corrosion of Arc-Welded Mild Steel. Moses[11]

	Penetration—Inches per Month											
	Maximum				Minimum				Average			
Reagent No.	*A*	*B*	*C*	*D*	*A*	*B*	*C*	*D*	*A*	*B*	*C*	*D*
1	1.460	1.560	1.500	1.392	1.045	1.112	1.025	1.080	1.239	1.304	1.178	1.234
2	1.350	1.272	0.836	1.298	0.564	0.499	0.423	0.473	0.912	0.893	0.670	0.718
3	1.104	1.253	1.205	1.040	0.546	0.538	0.443	0.462	0.747	0.751	0.735	0.630
4	0.185	0.183	0.177	0.183	0.096	0.059	0.075	0.062	0.151	0.147	0.122	0.145
5	5.20	4.82	5.06	4.98	3.02	2.91	2.68	2.33	4.33	4.04	3.95	4.05
6	0.0033	0.0029	0.0035	0.0023	0.0008	0.0010	0.0004	0.0004	0.0017	0.0018	0.0015	0.0011

A = Plate 2½ to 3 inches from weld
B = Plate adjacent to weld
C = All-weld-metal
D = Weld and plate

Table 8A—Range of Compositions of Materials Tested by Moses[11]

	C	Mn	Si	S	P
Base metal	0.09–0.33	0.40–0.83	0.011–0.09	0.012–0.043	0.015–0.033
Weld metal	0.06–0.13	0.36–0.77	0.084–0.37	0.009–0.033	0.014–0.048

in the following solutions contained in reflux flasks: (1) 85% H_2SO_4 (431° F. = 222° C.); (2) boiling 10% acetic acid; (3) boiling H_2SO_3 half saturated at 110° F.; (4) boiling 10% acetic acid, (5) boiling 10% HCl; (6) 26% NH_3 at 100° F. The time of heating and cooling was 20 to 30 min. Minimum, maximum and average values of penetration in inches per month determined on scrubbed samples are given in Table 8. The range of compositions of the 13 base metals and 15 weld metals used is shown in Table 8A.

There is no correlation between the analysis of base metal and weld metal and penetration, or between difference in composition of weld and plate, and penetration. Combination *D* of weld and plate gave slightly lower values of penetration than the other three combinations, except in acetic acid. A bare electrode weld in reagent No. 5 (Boiling 10% HCl) was penetrated at the rate of: *A*–3.85, *C*–11.15, *D*–8.10 inch/month. The value for base metal *A* is slightly less than average for coated electrode welds, possibly because the heat affected zone is narrower in bare electrode welds. But all-weld-metal (*C*) and weld-plate combination (*D*) corroded 2 to 3 times as fast as similar specimens from covered electrode welds. The rapid corrosion of bare electrode welds is attributed to iron oxide and nitrogen. The only oxide in covered electrode welds is silica, which is beneficial to corrosion resistance. Badly segregated inclusions promote galvanic action in welds, as shown by tests in 20% HCl at 130° F. for 3 hrs. A weld metal containing 0.40 Mo gave no advantage in Reagents 1 and 2.

Moses also subjected a 1⅜-inch plate containing a covered electrode weld to unsystematic, alternate immersion in pickling acids (H_2SO_4, HNO_3 and HCl) for over a year. The specimen was ⅛ inch thick after test, preferential corrosion not being observed.

1935—British I. & S. I. Corrosion Committee[12]

Butt-welded specimens (60° V, 1/5-inch root spacing 0.35 to 0.43 inch thick 12 inches long, and about 2½ inches wide) made with six different electrodes (no details) in mild steel and high-tensile steel, using 10 gage electrodes for first run, 8 gage for succeeding runs were exposed for 30 months under the following conditions: (1) wind and water; (2) sea air; (3) constant immersion in sea water at least 4 to 5 feet below surface; (4) ditto in contact with flat strip of brass. The mean tensile strength (psi) of all uncorroded and corroded specimens is shown in Table 9.

Table 9—Tensile Strength of Corroded Arc Welds. Corrosion Committee[12]

Specimen	Uncorroded		Corroded	
	Unwelded	Welded	Unwelded	Welded
Mild steel	55,100	53,100	51,700	49,300
High-tensile steel	82,600	66,500	76,500	63,600

The results showed that the effect of corrosion on the tensile strength was not greater for the welded than for the unwelded. The corrosion was uniform in all specimens, weld metal and heat-affected zones being corroded to the same extent as base metal, except in the welded high-tensile specimens which were slightly corroded preferentially at the junction zones. Specimens in contact with brass showed slightly concentrated attack in the junction zones. None of the electrodes showed outstanding superiority or inferiority.

1935—Zechnovitzer[255]

The loss in weight and change in tensile strength of arc welds in mild steel (plates contained 0.20–0.22 C, 0.48–0.60 Mn; deposited metal contained 0.05–0.13 C, 0.25–0.32 Mn, 0.08–0.41 Cu; plates 0.24, 0.39, 0.47, 0.71 inch thick; electrodes 0.16–0.24 inch diam., 160 to 220 amps. for 0.71-inch plate) were determined after continuous immersion for 3 to 90 days in artificial and natural sea water, tap water and a mixture of 10% HCl plus 10% H_2SO_4 at 18° C. The rate of corrosion for samples 0.39 x 1.57 inch was about the same welded or unwelded. The tensile strength of the welds decreased about 25% after 3 months in the acid mixture. Micrographs revealed that the welds contained considerable quantities of non-metallic inclusions.

1934—Hensel and Williams[13]

Polished all-weld-metal specimens (2 inches long, ½ inch diam.) prepared with the electrodes shown in Table 10 were subjected to alternate immersion in 1*N* HCl at 30° C. for 16 days. The results are shown in Table 11. The rate of corrosion of the bare and dust-coated electrode weld metal was still increasing with time

at the end of 16 days. The coated electrode metal had assumed a constant rate of corrosion. Bare and dust coated electrode weld metal developed severe pitting.

Table 10—Electrodes Tested by Hensel and Williams[13]

	C	Mn	Si	N_2	Total Residue
W—coated electrode 5/16 inch diam. 30 volt, 220 amps. D.C.	0.07	0.42	0.19	0.03 to 0.05	0.247
F—coated electrode 5/16 inch diam. 30 volt, 220 amps. D.C.	0.10	0.40	0.12	0.03 to 0.07	0.206
FA—bare electrode 5/16 inch diam. 17 volt 265 amps. D.C.	0.027	0.006	0.004	0.13	0.086
K—dust coated electrode 5/16 inch diam. 17 volt 265 amps. D.C.	0.03	0.08	0.012	0.126	...

Table 11—Alternate Immersion Tests (HCl) of Arc-Welded All-Weld-Metal. Hensel and Williams[13]

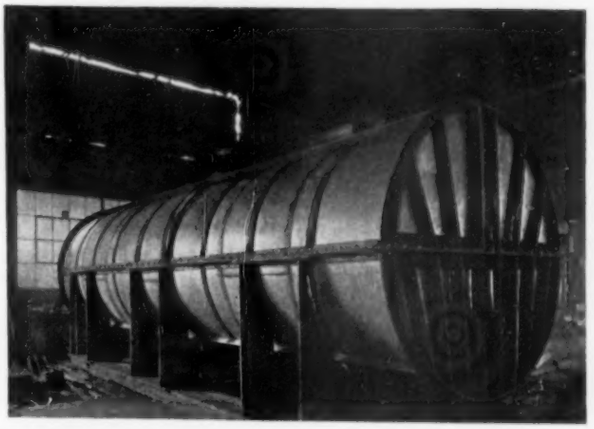
Electrode	Weight Loss in %	Comparative Weight Loss
W	20.13	1.00
F	21.64	1.04
FA	49.75	2.5
K	63.91	3.15

Reduced section tensile specimens (single-V welds) in 3/8-inch low-carbon steel plate were subjected to alternate immersion in 1*N* NaCl at 30° C. for 39 weeks and in 1*N* HCl for 7 days. The results are given in Table 12, and show that in 1*N* NaCl the strength of bare electrode welds was decreased proportionately less than that of coated electrode welds. An explanation

Table 12—Tensile Properties of Corroded Arc Welds. Hensel and Williams[13]

	In NaCl, 30° C., 39 Weeks			In HCl, 7 Days Tensile Strength, psi	
Electrode	Tensile Strength, psi Uncorroded	Corroded	Elongation % in 1/2 Inch Corroded	Computed on Uncorroded Area	Computed on Corroded Cross Section
W	71,000	51,800	16	29,000	52,300
F	72,600	56,600	16	33,300	51,500
FA	61,900	47,200	10	25,000	31,400

is not given. In HCl the inherent strength of the bare electrode weld was decreased 50%, whether by pitting or intercrystalline corrosion is not clear, whereas the inherent strength of coated electrode welds was decreased less than 30%. The degree of corrosive attack appeared to depend on the grain size of the overheated base metal



10,000 Gal. Pure Nickel Storage Tank for Phenol. Dimensions 26 Ft. Long x 8 Ft. Diam. Built of 0.109 Inch Nickel Sheet. Carbon Arc Welded Using "T" Nickel Rod. Built by The Whitlock Coil Pipe Co., Hartford, Conn.

in the junction zone, where preferential attack occurred. Columnar structure was less corrosion resistant than refined structure.

1934—Portevin[14]

Welded specimens in mild steel (57,000 to 64,000 psi tensile strength) made with bare and covered electrodes, and in high-tensile steel (85,000 psi) made with covered electrodes, were tested for 50 days at 20° C. in tap water, artificial sea water, and 15% H_2SO_4 by volume. The results shown in Table 13 indicate that the corrosion resistance of bare electrode welds is inferior to covered in fairly conc. H_2SO_4 but not in the other two reagents.

1933—Meunier[15]

Specimens welded from basic and acid steel (one-half of each specimen basic, the other half acid) were dipped once every three minutes in 3% NaCl solution for a year. Three kinds of corrosion were observed: (1) heavy preferential corrosion of the weld, caused by oxidation of metal from bare electrodes; (2) corrosion pits in weld, caused by massive oxide inclusions and differential aeration; (3) uniform corrosion of plate and weld in specimens welded with high-grade electrodes.

1933—Vanderpoorten[16]

All-weld-metal specimens prepared with mild steel electrodes were subjected to H_2SO_4 at various concentrations and temperatures. Multi-layer welds were more resistant than single-layer.

1933—Bright[17]

Specimens of bare electrode welds and bare and coated electrode welds in contact were tested in 12% and 20% H_2SO_4. The welds always had better corrosion resistance than plate.

1933—Anonymous[18]

Welded joints (single V) were subjected to alternate

Table 13—Tap Water Corrosion Tests of Arc-Welded Steel. Portevin[14]

	Loss of Weight—Gm./Dm.^{2}			
Specimen	Tap Water	Artificial Sea Water	15% H_2SO_4 Loss of Weight	Appearance
High-tensile steel, covered electrodes	0.620	0.70	6.8	Practically no localized corrosion
Mild steel, covered electrodes	0.630	0.70	20.4	More or less deep localized corrosion
Mild steel, bare electrodes	0.600	0.70	39.0	Weld deeply attacked; 1/3 corroded away

immersion in sea water for 34 months (over 200,000 dips). High-grade covered electrode welds showed no preferential attack at weld. Paste covered showed distinct preferential attack, and bare were very heavily corroded preferentially.

1932—Cockburn[19]

Welded joints made with bare and covered electrodes were subjected to alternate tidal immersion in Hawkesbury River, Australia. After two years joints made with both electrodes showed the same amount of corrosion.

1932—Colinet[20]

Alternate immersion tests in 3% NaCl solution for 4 months (48,934 dips) were made on metal arc welds. Bare electrode welds decreased in thickness 0.004 in. as a result of the test. There was no detectable decrease in thickness of welds made with good covered electrodes.

1931—Hodge[21]

The less and more soluble portions of metal arc welds in 5-inch plate that had been pickled in hot HCl (1:1) were analyzed chemically. The less soluble portions contained 0.037% N_2 and 0.001% residue. The more soluble portions contained 0.046 to 0.052% N_2 and 0.030 to 0.035% residue. Hodge deduced that the nitrogen and oxygen content of weld metal is an unimportant factor in corrosion.

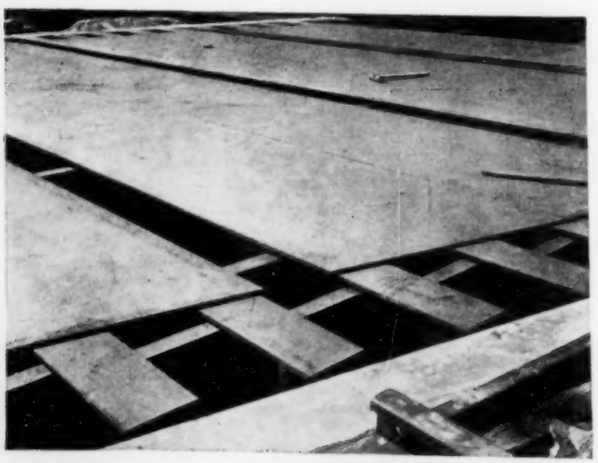
1931—Joellenbeck and Massmann[22]

All-weld-metal (metal arc, mild steel) lost as much weight as unwelded rolled steel in HNO_3 (1.2 sp. gr.) and HCl (1.19 sp. gr.) for 8 hrs. In 11% H_2SO_4 for 48 days the weld metal lost 20% more weight than unwelded rolled steel.

1931—Anonymous[23]

Arc welds in mild steel made with bare electrode containing special reducing agents and with a high-grade covered electrode were exposed to severe winter industrial atmosphere for two months. The bare-electrode-weld gained 0.49 to 0.73% in weight; the covered gained 0.43 to 0.56%.

Metal arc-welded mild steel immersed in 32½% caustic soda for two months was practically unattacked.



Welded Bridge Deck of Wrought Iron Plates. Courtesy A. M. Byers Company

1930—Sarazin[24]

All-weld-metal specimens made with a non-forgeable electrode, when immersed in HCl for 2 days (no details), lost 45% in weight; all-weld-metal deposited by forgeable electrodes lost 27.5% in weight under identical conditions.

1929—Moon[25]

Alternate immersion tests of bare and coated electrode welds (mild steel) in 1% H_2SO_4 for 6 hrs. followed by 18 hrs. in air for a period of 3 months caused almost complete destruction of the bare electrode joint, but the covered electrode weld showed little attack.

1927—Kirkland[26]

Joints welded with bare and covered electrodes were subjected to laboratory alternate immersion tests in sea water. After 20 months (over 200,000 dips) the weld metal of the bare electrode joint had almost wasted away but the covered electrode weld showed no preferential attack.

1925—Owens[27]

Butt-welded plates 6 inches square (metal arc process, joint at center) were attached to the sides of several ships and observed for 18 months. There was no difference in corrosion between weld and plate metal.

Alternate immersion tests in sea water and gasoline (1 week in sea water, 1 week in gasoline) for 6 months also showed no preferential corrosion in metal arc-welded mild steel.

1924—Neubert [41]

Welds made by different operators in steel containing 0.420 C, 0.60 Mn, 0.05 Si, trace P and S, with an electrode containing 0.25 C, 0.3 Mn, 0.05 Si, 0.05 S, 0.05 P, with a coating containing 60% $CaCO_3$, 35% Na_2CO_3, 5% Si were subjected to 1% H_2SO_4 for 562 hours. The loss in weight with low-grade welds was 44%; with high-grade welds the loss was only 16%.

1921—Unland[378]

Welded sections of iron pipe were exposed for three weeks to a continuous spray of hot salt water. After test, the layer of oxide on base metal was 0.02 inch thick, and was materially thinner on the welds.

1919—Gard[377] (Lloyd's Portsmouth Tests in 1917)

Arc welds, fillet and butt, retained strength after a year in sea water.

A number of writers[28] have made more or less general statements to the effect that covered electrode welds are much more resistant to particular types of corrosion than bare electrode welds. However, Bauer, Kröhnke and Masing[29] in their text on corrosion (1936) state that bare electrode welds in mild steel are no less corrosion resistant than unwelded metal, a statement that the present review does not substantiate. According to Moore,[30] welds made with bare low-carbon electrodes (short arc) corrode as fast as base metal (ship steel). Covered electrode welds are more corrosion resistant than base metal, which is confirmed by *Arcos*.[31] Moore points out that there is no evidence to show that fuel oil attacks weld metal more rapidly than base metal. Sidorischin[32] mentions two kinds of coated electrode that did not give very satisfactory welds from the corrosion standpoint.

Electrochemical Potential of Arc Welds

The difference in solution potential between weld metal and base metal—a difference that may be, and

often is, minute and negligible but that is extremely difficult entirely to avoid—was made the basis of an explanation by Armstrong[33] and others (1916–1917) for the superior corrosion resistance of welds made with slag-coated electrodes. According to Armstrong, bare electrode weld metal is distinctly electro-positive to surrounding base metal because weld metal is less pure, containing Fe_3O_4 and other impurities. Weld metal deposited by slag coated electrodes is, on the other hand, electro-negative to base metal. Corrosion therefore is prevented to a very large extent. (Note: Throughout the review the terms electro-positive and electro-negative have been used in the same way as the investigators themselves used them. Present usage is to apply "electro-positive" to noble metals such as gold, and "electro-negative" to base metals, such as zinc. Consequently, Armstrong's use of the terms is the reverse of present practice.

Recent investigations on the electrochemical problem in the corrosion of arc welds in mild steel cannot be said to advance the solution much beyond the preliminary stage reached by Armstrong. The reason for the comparatively meagre developments in the electrochemical theory of the corrosion of arc welds is made plain by Brillié, Leroy and Roux,[3] who determined the potential of the cell: base metal *vs.* all-weld-metal using polished specimens (0000 emery paper). The materials and reagents were the same as those described in the section on Combined Experimental Investigations. The electrodes (base metal 0.4 inch square, all-weld-metal 0.4 inch diam., both $2^3/_8$ inches long) were suspended 2 inches apart by glass hooks. The electrodes dipped 0.4 inch beneath the surface of the acid solution (100 ml. 20% H_2SO_4) and 0.8 inch beneath the artificial sea water (100 ml.). The upper 0.4 inch of the immersed length of the electrode in sea water was varnished to obviate water line effects (differential aeration) which disturbed the readings. Assuming not unreasonably that the anodic specimen is the one that should suffer preferential corrosion in tests, Brillié and coworkers found that the potential measurements were sometimes in agreement with, sometimes exactly opposite to, the corrosion test results. They give five reasons for the absence of agreement:

1. all-weld-metal specimens do not have the same composition as the weld metal in an actual joint;
2. in welded joints plate and weld metal are short circuited, whereas for potential measurements the circuit must be open;
3. small pits, which were of no importance in corrosion tests, had a marked influence on potential measurements (in what way is not stated);
4. corrosion products do not adhere uniformly;
5. when a couple is formed in a joint the action is local in character, and there cannot be much difference in potential between the two spots.

Like the above investigators, Schoenmaker and Blokker[8] found that potential measurements in a solution containing 3% NaCl and 0.1% H_2O_2, of all-weld-metal specimens with respect to base metal (see the second investigation listed under "Experimental Corrosion Tests of Metal Arc Welds") were inconclusive, the electrode deposits showing much greater positive potential differences with respect to low-alloy steel than to mild steel. Weld joints (90° X) in plates $2^3/_4$ x $^{13}/_{16}$ x 0.32 inch thick were therefore tested. A gold wire made contact with the weld metal, and a calomel electrode dipped close to base metal. The solution contained 3% NaCl and 0.1% H_2O_2. The results in Table 14 show that the best results were obtained with the heavy coated Resistens electrode in both mild steel and low-alloy steel. The Generalis electrode was thin covered, and had a tensile strength of 58,000 psi, 17.2% elongation.

Slutska[10] found that the corrosion of welded and riveted joints (see Slutska—section on Corrosion of Metal Arc Welds) is not uniform. The joints were therefore explored with a calomel electrode (Poggendorf's method) in the solution used for corrosion testing (160 gm. NaOH plus 200 gm. NaCl in 1 liter of water). The maximum potential developed on the welded sample was 0.75 volt, on the riveted sample 0.70 volt. But the potential pattern of the riveted joint was much more complex than that of the welded.

The specimens were also explored in a non-passivating solution, 1 *N* with respect to ferrous ion. The potential difference between different parts of the riveted joint was as much as 0.20 volt; for the welded joint the maximum difference was only 0.05 volt.

In Roux's[34] opinion, the potential of the cell: base metal/corroding solution/all-weld-metal as a function of time, the specimens being 0.15 inch^2 exposed area and being connected to a galvanometer, offers an excellent indication of corrosion behavior of welds in electrolytic conditions. Roux gives four examples of the application of the method to metal arc welds.

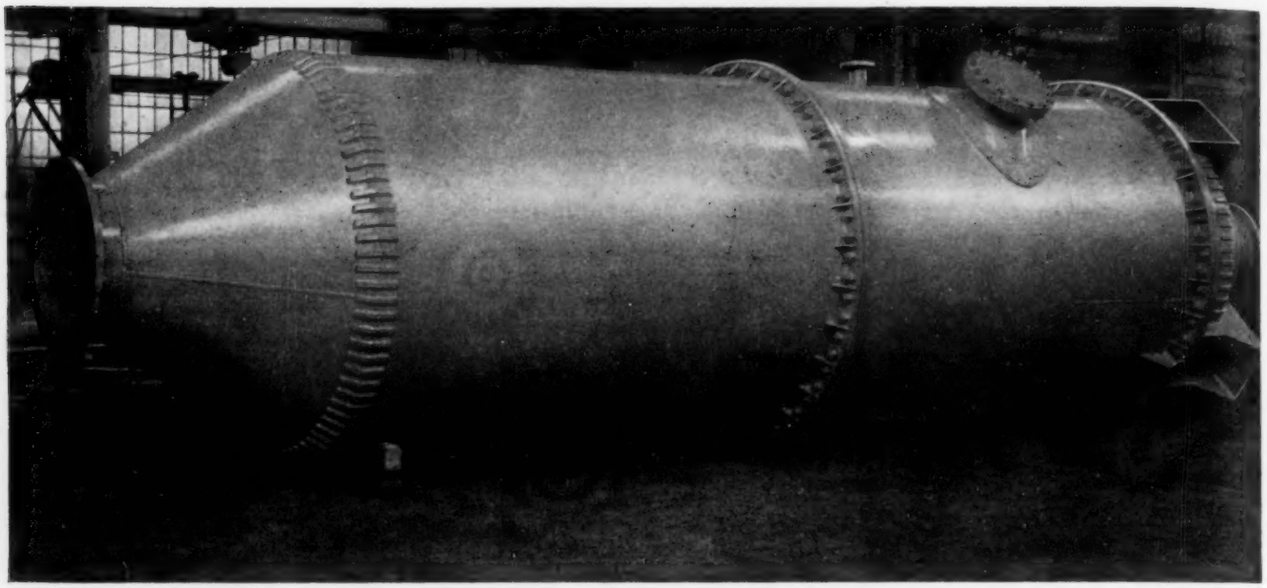
1. In the cell: low-carbon steel/4% H_2SO_4/weld metal (<0.25 C, 0.1 to 0.8 Mn) the weld metal is anodic, the emf. increasing with time and eventually becoming constant. In 2% HCl the weld metal is first anodic, but after 5 minutes the cell reverses polarity, the emf. eventually becoming constant.

2. If weld metal contains <0.2 C, 1 to 2 Ni, or 0.5 Cr, 0.5 Cu, it is cathodic, the emf. decreases with time to reach a constant value.

Table 14—Potential Measurements on Arc-Welded Steel. Schoenmaker and Blokker[8]

Series	Position of Specimen	Average Difference in Potential—Millivolts 5	10	16	19	21 Days
Mild steel, Resistens electrode	Horizontal*	+15	−5	−5	0	0
Mild steel, Resistens electrode	Vertical*	..	...	+1	0	0
Mild steel, Resistens electrode	Vertical*	..	...	−2	+5	0
Mild steel, Generalis electrode	Horizontal	+29	+4	+6	+17	+9
Mild steel, Generalis electrode	Vertical	..	...	+8	+5	+5
Mild steel, Generalis electrode	Vertical	..	...	0	+ 8	+5
Mild steel, bare electrode	Horizontal	+25	+9	+9	+26	+6
Mild steel, bare electrode	Vertical	..	...	+2	+ 5	+5
Mild steel, bare electrode	Vertical	..	...	+1	+11	+6
Low-alloy steel, Resistens electrode	Horizontal	0	−1	−2	0	0
Low-alloy steel, Resistens electrode	Vertical	..	...	+2	0	0
Low-alloy steel, Resistens electrode	Vertical	..	...	0	0	0

* (Specimen was horizontal when X lay flat.)



A 7 Ft. Diameter by 30 Ft. High Nickel-Clad Steel Evaporator for Concentration of Caustic Soda. It Is Entirely of Welded Construction with Both 10 and 20% Nickel-Clad Steel Used in the Construction. 2 In. Diameter Pure Nickel Tubes Serve to Transfer the Heat from the Steam to the Caustic Soda

3. If base metal contains <0.2 C, 1 to 2 Ni, or 0.5 Cr, 0.5 Cu, and weld metal is unalloyed low-carbon steel, the weld is anodic in H_2SO_4, the emf. increasing with time to reach a point of stabilization.

4. If alloy steel base metal and weld metal is used, the behavior is the same as in (3).

The time of test in all cases was 25 minutes. No correlation with actual corrosion behavior is offered by Roux.

Meunier[16] showed that the electrolytic potential of metal arc welds in 0.1 *N* KCl with respect to calomel electrode (0.1 *N*) varied with Charpy notched bar impact value. Thus weld metal with a Charpy value of 0.8 mkg./cm.^{2} had an emf. of −0.77 volt (18° C.); with 10 mkg./cm.^{2} the emf. was −0.73 volt. The plot of Charpy value *vs.* emf. was a curve that was practically horizontal at the higher Charpy values. In view of the rather wide scatter of results, Meunier's discovery can hardly be of more than qualitative value, indicating that highly-oxidized (bare electrode) weld metal corrodes by local action with non-oxidized plate, which has a more noble potential. Meunier states that pitting at oxide inclusions is due to differential aeration, not to electrolytic action.

The potential difference between base metal and weld metal as a function of time and intensity of aeration in sea water and a mixture of 10% HCl plus 10% H_2SO_4 was measured by Zechnovitzer[255] (see Section on Experimental Investigations of Metal Arc Welds). The difference varied between −0.170 to +0.065 volt, depending on quality of weld. As the weld metal becomes more electro-positive with respect to base metal the corrosion is decreased.

The ferroxyl indicator has been used by Moses[11] to show the great difference in potential between weld and base metal in welds made with bare or low-grade covered electrodes, the weld metal turning the indicator blue. There was no marked difference in potential between weld and base metal in covered electrode welds.

Corrosion of Arc Welds in Service

The behavior of arc welds in ships has been excellent. After 11 years of service the welded seams of the motor vessel Fullagar[35] showed no undue corrosion. Bissell[36] also had no difficulty with the corrosion of welds on the bottoms of ships. Shaw[37] observed no appreciable reduction of thickness of welds in two arc-welded vessels after the shell plating had been immersed in salt water for 4 years. According to Owens,[38] excessive corrosion does not occur in welded piping and hangers in oil tanks or elsewhere on ships. Naumov[39] and Danninger[40] report good corrosion resistance of covered electrode welds in cellulose sulfate digesters. An arc-welded mild steel pulp digester (39 feet long, 20 feet diameter) was subjected to wood products in steam at 100 psi for 6 to 7 hours by Kostersitz.[42] There was considerable rust on the plate but none on the weld after test, which showed that the oxide (slag) on the weld was more corrosion resistant than ordinary mill scale. Corrosion results from high-temperature, high-pressure vessels handling very corrosive crude and gas oils show consistently, according to Jasper,[43] that weld metal is no more subject to corrosion than base metal. He cites a pressure vessel which was direct fired on the outside and contained corrosive liquid. During $3^1/_2$ years of continuous operation the wall thickness was reduced from 2.5 to 1.6 inches. A test to destruction on the corroded vessel developed 56,000 psi rupture stress. The welds (no details, presumably high-grade covered electrodes) in the highly corroded sections had corroded much less than the plates themselves. After 5 years of service the arc-welded patches of a corroded boiler inspected by Reichner[44] showed no rust.

H. P. Smith[45] reports the measurements shown in Table 15 on 8 inch 90° standard weight steel pipe bends with metal arc-welded joints. The pipe line had been in service for 13 months carrying highly corrosive and erosive crude oil vapor at 850° F., 50 psi pressure, and

Table 15—Corrosion of Arc-Welded Pipe in Oil Refinery. H. P. Smith[45]

	Pipe Bend	Pipe
At time of installation, wall thickness throughout	0.322 in.	0.322 in.
When removed, wall thickness on outside of bend throughout	0.101 in.	0.062 in.
When removed, wall thickness on inside of bend throughout	0.181 in.	0.062 in.

extremely high velocity. The welds were corroded to the same extent as pipe. The estimated life of a threaded joint was 7 months.

Poor service corrosion behavior of metal arc-welded wrought iron pipe joints in technical H_2SO_4 as well as in ammoniacal steam containing small amounts of sulphurous acid is reported by Gross.[46] Riveted or flanged joints had much better corrosion resistance. Gross attributed the unsatisfactory service to inclusions of iron oxide in the welds. A bad weld[383] often has poor corrosion resistance. Bauer[47] states that cracks in cast-iron caustic soda pots operating at 1000° F., when welded with covered, mild steel electrodes, showed less corrosion than the cast iron itself. An interesting example of good corrosion resistance of welded rivet heads in a hot water heater is described by Monroe.[45] After 10 years of service the weld metal showed less corrosion than the rivet heads. According to Ritchie,[384] although an arc-welded, mild steel stopper for a gas retort had a life of only six months exposed to water and sulphur fumes, the weld metal showed less attack than base metal.

Corrosion Tests of Gas Welds

Laboratory corrosion tests on 6-inch pipe joints oxyacetylene welded with the three most typical combinations of rod and welding technique are very briefly described by Hutchison.[50] The tests included exposure for one year to atmosphere, soil immersion, tap water and atmosphere, and salt spray, as well as short time tests in HCl and short time electrolysis tests in salt water (no details). There was no appreciable difference in corrosion resistance among the welds or between the welded and unwelded pipe.

Leroy[49] states that oxyacetylene welds corrode more uniformly than arc, and that slag inclusions and flux are damaging to the corrosion resistance of gas welds. According to Krynes,[51] the corrosion resistance of gas-butt welds in mild steel is improved by reverse welding. Tension tests, of which Meslier,[52] who refers to them, supplies no details, of gas-welded steel pipes reduced in weight 10% by corrosion showed remarkably high strength. Granjon[53] stated that preferential corrosion of oxyacetylene welds in mild steel was caused by impurities in the weld, particularly at the exposed surfaces, which could be eliminated by proper welding technique.

The corrosion resistance in service that may be expected of gas-welded pipe joints is discussed by Hutchison[50] and Oxy-Acetylene Tips.[54] The welded joints were never unduly affected by corrosion during long periods even though practically the entire length of the pipe was pitted. Greger[55] mentions oxyacetylene welds in boiler plate that completely withstood caustic embrittlement conditions in highly concentrated lye.

Corrosion Resistance of Carbon-Arc Welds

According to E. W. P. Smith,[56] carbon-arc welds in mild steel made in a shielding gas have better general corrosion resistance than unshielded. Moses[11] found that two-pass carbon-arc welds in $^3/_4$-inch mild steel plate, when tested in Ferroxyl indicator, showed no definite difference in potential due to differences in grain size. That is to say, the fine-grain part of the weld had the same potential as the coarse-grain. Carbon-arc welds did not have satisfactory corrosion resistance in the tests performed by the British Engine, Boiler & Electrical Insurance Company.[57]

Corrosion Resistance of Forge Welds

Reavell[58] cites a water-gas welded vat for caustic soda under 80 psi which lasted 12 years. It is Buchholz's[59] opinion that forge welding is the best process for mild steel chemical apparatus, but there are few who will agree with him. A forge weld in boiler plate containing 0.033 P, 0.061 As, 0.085 S, which failed by corrosion aggravated by poor welding, is described by Bauer.[60] Nehl[61] subjected forge welds in mild steel to a 5% solution containing HCl and H_2SO_4 for 60 days, and found that the welded zone showed less attack than base metal. In reply to a statement that water-gas welds in general are selectively corroded, Daeves[62] quotes corrosion tests in 20% HCl. Forge welds in unalloyed open-hearth steel lost 69% in weight after 30 days, the majority of the loss occurring in plate. Forge welds in a special boiler steel (no details) lost only 9.9% in weight. Moses[11] observed that entrapped slag once exposed causes rapid corrosion of forge welds.

Corrosion Resistance of Electric Resistance Welds

Practically the only information on the corrosion of resistance welds in mild steel is given by Rawdon and Waldron.[63] Butt-resistance and flash welded 2-inch open-hearth steel pipes ($^3/_{16}$-inch wall, 0.1 C, 0.4 Mn, 0.1 to 0.3 Cu) were subjected to continuous-flow corrosion tests in softened and unsoftened tap water for 6 weeks. The flash welds were planished off inside. The corrosion of the specimens was uniform; there was no preferential corrosion at the welds. Willoughby[64] describes the satisfactory service, especially in respect to corrosion, given by a spot-welded freight car in operation from 1911 to 1927. No trace of rust was observed by Helsby[65] in spot-welded black sheet partitions (mild steel) after 30 years of service in a London hospital. Bodiansky[66] found that rust between spot-welded mild steel sheet can be prevented by painting the sheet beforehand with red lead. The sheets are spot welded before the paint is hard (6 to 12 hrs. after painting), the electrode pressure forcing out the paint.

General Service Corrosion Results

In addition to the service corrosion results of particular kinds of welds already discussed, several examples of the corrosion resistance of mild steel welds in general have been reported. Before examples are cited, however, it may be well to note the absence of corrosion trouble due to welds in pipe lines. Heltzel,[67] for instance, remarks that welding is a means for repairing corrosion damage in oil pipe lines, such as at leaky thread couplings, but is not a cause of corrosion. Bly[68] also states that welding in pipe lines gives less corrosion trouble than hand or machine screwed couplings, but recommends a protective coating on the weld and adjacent areas. According to Kuhn,[69] the current distribution along the entire length of a pipe line with welded joints may be considered uniform. For other types of joints there are discontinuities in current distribution at the joints. The statement by Gibrat[70] that leakage currents from street railway rails are appreciable with ordinary rail joints, but are negligible with Thermit welded joints is interesting in this connection. A 6-inch 90° welded tube turn in an oil refinery was found by Curley[71] to have been corroded to as thin as 0.011 inch at spots (orginal wall thickness 0.28 in.), but there was no indication of accelerated attack at the welds. In 1918, Gerhard[72] found, from inspection of drain pipes in New York buildings, that welded steel pipes were not nearly so good in corrosion as wrought iron. Examples of poor corrosion resistance of low-grade welds were given in a discussion of an article by Calcott and coworkers[73] in 1923.

The British Corrosion Committee[74] observed that in a few instances fillet welds in the conveyors of a sugar beet factory failed very rapidly by corrosion. Perhaps the

explanation may be found in Hunter's[75] statement that intermittent welds are more subject to corrosion than continuous welds. Owens[76] cites several instances in which welded marine structures showed inappreciable corrosion, and Füchsel[77] describes some welded submarine structures which were still in satisfactory condition after 5 years of service in harbor water.

The behavior of 6-inch lengths of 1-inch butt-welded steel pipe buried in white alkali soil in California for 1 year was recorded by Christopher.[78] The butt weld along the length of the pipe seemed to open up, and pitting was observed along the seam. It is stated that pipes in service in corrosive soil develop the same defect.

Caustic Embrittlement

As Thompson and Jeavons[79] point out, caustic embrittlement is not encountered in good welding, nor are there corrosive effects due to cold work. Service results entirely confirm their statement. Nevertheless, Gareis[80] has observed cracks parallel to the welds in the heat-affected zone of gas-welded pipes carrying crude gas containing quantities of hot basic residues, which are known to cause caustic embrittlement. Although it appears that cracks have been observed in such circumstances for a number of years, no adequate explanation has yet been found.

Liedloff[81] also believes that caustic embrittlement may occur in heavily stressed or cold-worked welds. He observed caustic embrittlement in the welds of an oxyacetylene welded wrought iron tank 0.28 to 0.35 inch thick subjected to hot NaOH solution 40° Bé. The embrittlement is due, in his opinion, to high residual stresses set up by incorrect welding sequence. Fink[405] observed strain corrosion lines in the heat-affected zone of corroded, welded I beams.

A German boiler periodical reports stress-corrosion tests of normalized and stress annealed V butt welds in low-carbon steel.[82] The welds were reverse welded (heavy coated mild steel electrodes) and were tested for 32 hours in a boiling corrosive liquid (3% NH_4NO_3, 60% $Ca(NO_3)_2$, 37% H_2O), the face of the weld being in tension. The weld metal in the normalized welds developed serious intergranular cracking whereas the stress annealed welds did not. It was thought that the grain boundaries in the normalized welds were more favorable to intergranular penetration than the finer-grained, stress annealed welds.

Kautz[83] reports the following results for austenitic butt welds (25 Cr, 20 Ni) in $^1/_2$-inch mild steel plate (Izett) subjected to stress-corrosion tests in $NaNO_3$ plus H_2O (2:1) at 95° C. Out of 20 specimens (not heat-treated), one failed after 13 days, the remainder were still intact after 20 days. Welds in ordinary boiler plate made with unalloyed high-grade electrodes failed by fracture near the weld after 6 days under the same conditions. Some of the mild steel welds were tested without heat treatment, some were heat-treated $^1/_2$ hr. at 930° C., air cooled.

Caustic embrittlement was not observed by Kleiner[84] in his tests of oxyacetylene flame hardened brake drums subjected to 50% NaOH solution for 10 weeks at 90° C. under a fiber stress of 28,500 psi. The drums contained 0.330 C, 0.010 Si, 0.580 Mn, 0.019 P, 0.029 S, 0.120 Ni, 0.060 Cr and were tested unhardened (120 Brinell) as well as in two stages of hardening (320 and 500 Brinell). Kleiner did not determine corrosion loss but believed that hardening would exert a protective effect if corrosion were prolonged.

Effect of Grain Size

The effect of grain size on the corrosion resistance of welds in mild steel is still not clearly known, as Zimm[85] points out.

An investigation by Séférian and Leroy[398] on arc-welded specimens of dead mild steel, medium-carbon steel (0.33 C), copper steel (0.40 Cu) and chromium-copper steel (0.52 Cr, 0.57 Cu) showed that, in acid media, pronounced corrosion occurs at the junction of unaffected base metal with the heat-affected zone. At the junction fine grains are mixed with coarse, and some troostitic material is probably present. Flame-cut surfaces developed a corrosion line at the same junction. In oxyacetylene welds conditions were more complex. The overheated, Widmannstätten zone was most susceptible to corrosion, and the line of junction of unaffected base metal with the heat-affected zone was also susceptible. The intermediate annealed zone was relatively corrosion resistant. It is not clear to what extent the grain size effect was intensified or masked by structural changes in the steels.

Frantz[86] and Sillars[87] believe that non-overheated base metal corrodes more rapidly than the overheated, coarse-grained junction zones on account of electrolytic action. The B. E., B. & E. I. Co.[57] found that normalizing tends to reduce the corrosion of any weld, but they did not ascribe the effect definitely to change in grain structure or size. If present, the grain size effect is probably negligible, although, as Leroy and Bonnot[88] point out, uniform microstructure may be desirable.

Effect of Differences in Composition

Lynn[89] states that an endeavor is often made in boiler construction to deposit weld metal of the same composition as base metal. Krüger,[90] however, is strongly of the opinion for which the preceding review offers considerable support, that even large differences in composition between base metal and weld metal may not lead to local effects, and cites the corrosion resistance of Krupp austenitic welds in mild steel. Butt-welded Izett (mild) steel using the Krupp austenitic rod (Nichrotherm, 25 Cr, 20 Ni, 0.1 C, 1.3 Mn, 0.8 Si) was subjected for $3^1/_2$ months to continuous or alternate sea water immersion by Fry,[91] who detected no preferential attack of plate near weld. Carius[92] reports similar tests carried out at 20° C. in artificial sea water for 5 months. The results in Table 16 show that the Nichrotherm austenitic weld metal does not cause electrolytic action. The base metal is uniformly corroded, the

Table 16—Corrosion Tests of Mild Steel Welded with Austenitic Electrodes. Carius[92]

Kind of Test	Specimen	Surface Area Cm.² Austenitic Weld Metal	Surface Area Cm.² Izett Steel	Corrosion Rate Gm./M.²/Hr.
Continuous immersion	Butt weld	3.2	22.4	0.109
Continuous immersion	Normalized, unwelded Izett steel	...	11.3	0.105
Continuous immersion	Izett steel stretched 4% in tension	...	11.8	0.103
Alternate immersion	Butt weld	5.7	27.5	0.685
Alternate immersion	Normalized, unwelded Izett steel	...	11.2	0.669
Alternate immersion	Izett steel stretched 4% in tension	...	11.5	0.670

austenitic weld metal being unattacked, of course. The corrosion rate in the above table is expressed in grams per hour per square meter of exposed Izett steel. Specimens of Izett steel (0.26 C, 0.09 Si, 0.6 Mn, 0.029 P, 0.01 S) and Nichrotherm austenitic weld metal were also placed in and out of contact in boiler water in a 210 psi boiler for 128 days. There was no significant difference between specimens in and out of contact, which confirmed measurements of the electrode potential of the cell: austenitic weld/boiler water/Izett steel. The current density in the cell was 6×10^{-7} amp./cm.^{2}, which was the same as the potential of the cell normalized Izett steel/boiler water/Izett steel stretched 0.23% or 4% in tension. Since the ratio of boiler surface to Nichrotherm weld metal in an actual boiler is 1:50 to 1:100, the decrease in weight due to the slight local current flowing is found to be 0.012 to 0.006 gm. per year. High-oxygen content in the boiler water did not appreciably increase the rate of corrosion of the welded joint. Carius's results prove that difference in composition between weld metal and base metal does not necessarily imply poor corrosion resistance.

Iskra[93] states that soft steel bars to which hard steel plates were welded were not resistant to technical solutions of ethylene chlorhydrin (CH_2ClCH_2OH). The B. E., B. & E. I. Co.[57] also observed heavy corrosion at the resistance welded joints between two pipes containing 0.10 and 0.22% C, respectively. Overheated structure and oxide inclusions were contributing factors.

Copper steel rivets are sometimes used in mild steel plate to make the plate anodic and prevent local attack of the rivets. The use of copper steel filler rod for a similar purpose has not been recommended.

The general problem of contact corrosion has been discussed by Hippensteel,[94] among others. He clamped pairs of different metals together and exposed them to 9 types of atmospheric corrosion. The metals tend to accelerate the corrosion of other metals with which they are coupled roughly in the following order: Ni, Sn, Cu, Fe, Pb, Al. It should be noted that the conditions in these and all other similar tests do not duplicate conditions in a welded joint, and that no conclusions regarding welds can be drawn legitimately from corrosion tests involving mechanical contact alone. The difference in corrosion behavior of riveted and welded joints is an example. In many instances the influence of a metal in contact with another on the rate of corrosion is not even in the direction indicated by the electromotive series.

Effect of Impurities

The adverse effect of the oxygen and nitrogen content of mild steel weld metal on corrosion resistance is shown by Hodge[21] and Moses[11] in the section on Experimental Results. Goos[95] also believed that oxygen and nitrogen accounted for the rapid failure by corrosion of several welded rudders in sea water. Local attack, as shown by evolution of gas bubbles at the junction zones of welds in mild steel is always heaviest in welds containing oxide and nitride, according to the British Engine, Boiler & Electrical Insurance Co.[67] Inclusions of slag at the junction zone always lead to heavy attack, and act as nuclei for electrolytic action. Thom,[96] Ornitz,[97] and Zimm[35] also regarded slag and oxide inclusions as unfavorable to corrosion resistance.

Whitney[38b] showed that segregation lines in mild steel plate 1 inch thick develop large pits at junction with weld metal after 6 hrs. in hot 50% HCl. Leroy[39?] also emphasizes the effect of oxides in initiating pitting in welded mild steel chlorine tanks.

The effect of corrosion of filler rod or base metal on subsequent welding has been noted. Saxton[98] states that corroded bright drawn welding rods have a coating that is more detrimental to fluidity during welding than rods having a mill scale finish. However, evidence from another source indicates that it may be the cold drawing itself that is detrimental. Znaichenko and Timofeev[99] determined the mechanical properties of arc welds in mild steel plates that were given the following treatments prior to welding: (1) original steel, without special treatment, (2) metal covered with H_2SO_4, (3) same as 2 but washed with water, (4) same as 2 but neutralized with NaOH and washed with water. The best mechanical properties were obtained with 1 and 4, indicating the necessity for neutralization of acids on the steel before welding is attempted.

Mild Steel Welds vs. Rivets in Corrosion

Whatever difference there may be between the corrosion resistance of riveted and welded joints under corrosive conditions appears to be in favor of the welded joint. The question is left open by Hunter[100] and Deutsch,[101] and is decided against welding for tank cars by Frary[102] and the I. C. C. on the basis of cost. On the other hand, it is Pierce's[103] experience that welding eliminates corrosion in the shell plating of ships, and Ashworth and May[104] prefer welding to riveting in railway car construction for the same reason. Quevedo[105] intentionally seals outdoor fillet joints to prevent ingress of moisture to crevices which cannot be reached by paint brush or spray. Also for the purpose of eliminating crevices, Pau[106] strongly recommends that mild steel electroplating tanks be double welded, not riveted. The results of Slutska's[10] experiments were decidedly in favor of welded joints (see section on Experimental Investigations of Metal Arc Welds).

The origin of corrosion in the crevices of riveted joints, and in general at regions of mechanical contact between similar or dissimilar metals and non-metals, is ascribed by Evans to differential aeration and by Carius[107] to the increase in solution potential of iron in capillaries filled with liquid, giving rise to the formation of local elements. An instance in which the leak tightness of welded vessels with corrosion resistant lining is not desirable is given by Kostersitz.[42] Once the lining is punctured the riveted vessel gives notice by leaking at the seams; the welded vessel gives no indication under similar circumstances.

Corrosion Tests for Welds in Mild Steel

Besides the tests described in the experimental section,

Forty-Two of the Girders Supporting the Roof of the Oswego Tunnel on the New York Central Railroad Near Oswego, N. Y. Were Repaired the Latter Part of 1933 by Replacing the Old Cover Plates with New 1/2-Inch and 1/4-Inch Wrought Iron Plates. Courtesy A. M. Byers Company

a number of routine corrosion tests for welds in mild steel have been suggested. Thom[108] recommends a continuous acid immersion test using a butt-welded specimen with 1 inch of base metal on either side of the weld. The weld must show no greater attack than base metal. It must be admitted that observation of preferential attack in corrosion tests of welds is usually more informative than any sort of weight-loss measurement. Another important item of information, as Lohmann[5] points out, is the thickness and type of mill or weld scale on the specimen.

The German Marine[376] consider that the alternate immersion test in weekly corrosive media is too time-consuming. Ordinary butt-weld specimens are not suited for quantitative results because the proportion of surface of weld to base metal changes during test. In recommending the method of Searle and Laque (A. S. T. M. Proc. 35 (1935)), the German Marine advocate specimens in the form of disks $2^1/_4$ inches diameter with $^3/_8$-inch hole. The disks are 0.12 inch thick at the hole and taper to 0.04 inch at the edge. The specimens are strung on a rod with bakelite separators and immersed in beakers.

A microscope (30 to 70 magnification) is described by Kufferath[109] for comparing a standard corroded weld sample with a test specimen in the same field of view. Howard[110] and the Westinghouse Co.[111] have described alternate immersion tests. In Howard's test, welded tensile specimens are dipped in 1*N* HCl, the cycle being 1 minute in acid, 1 minute in air. The maximum loss of tensile strength of the specimens after 7 days should average not over 30%. Portevin[112] has evolved a method for computing weldability on the basis of loss-of-weight measurements on base metal and weld metal specimens. The method requires an arbitrary numerical statement of the relative importance of corrosion resistance compared with tensile strength or other properties. The importance of service corrosion tests, as distinct from laboratory tests is emphasized by Spraragen.[113] The protruding reinforcement of welds often concentrates rubbing action in service on themselves, whereas it is the nature of many applications of welding that the weld does not receive its share of cleaning through being located in the corners of, say, tanks. In either case laboratory corrosion tests of welded samples could hardly be expected to provide adequate information.

Corrosion of Welds in Low-Alloy Steels

Surprisingly little information on the corrosion of welds in low-alloy steels is available. The tests of Brillié, Leroy and Roux,[3] and of Schoenmaker and Blokker,[8] described in the experimental section of the review of mild steel, provide a limited amount of data. The only other investigator who reports results on low-alloy steel appears to be Stäger,[114] who determined the gain in weight and loss of tensile strength of welded specimens due to corrosion in a closed box containing: (1) air (100% humidity), condensation being produced by change of temperature, (2) an atmosphere of air and saturated steam. The base metal was unalloyed and copper-alloyed mild steel, 0.2 and 0.4 inch thick. The electrodes were unalloyed Stabilend (trade name) and copper-Stabilend. Metal deposited by the latter electrode contained 0.1–0.12 C, 0.6 Mn, 0.04 Si, 0.5–0.7 Cu. The tensile and impact properties of welded specimens are given in Table 17. Tests of all-weld-metal in both corrosive atmospheres showed the superiority of the copper-containing electrode. In the corrosion tests of butt welds, the gain of weight was determined on the entire specimen, but small specimens were cut out for the tensile tests.

Table 17—Tensile Properties of Uncorroded Welds. Stäger[114]

	Tensile Strength, psi	Elongation, % in 5 Diam.	Charpy Impact, Mkg./Cm.^{2}
Ordinary-stabilend	66,000 to 74,000	26–30	7–8
Copper-stabilend	71,000 to 78,000	24–31	8–9

The results of a test for 182 days in an atmosphere saturated with water are given in Table 18. Elongation of the welded specimens was not determined because fracture occurred at several places with respect to the weld. Although the mechanical properties of the specimens were practically unaffected by corrosion, the relatively short time of test revealed definite differences in gain of weight, despite the fact that the formation of a protective copper coating on copper-bearing steel requires time. (Stäger does not discuss the hypothesis that the effect of copper may not lie in film formation, but rather in the production of relatively non-hygroscopic corrosion products.) Specimens welded with the copper-bearing electrode gained less weight than those welded with unalloyed electrodes.

Table 18—Corrosion Tests of Welds in Low-Alloy Steel. Stäger[114]

Steel	Electrode	Increase of Weight, Gm.	Elastic Limit, psi	Tensile Strength, psi
Unalloyed mild steel	Copper-stabilend	0.54	51,400	73,200
Mild steel containing Cu	Copper-stabilend	0.23	44,700	70,800
Unalloyed mild steel	Ordinary-stabilend	0.79	52,300	71,000
Mild steel containing Cu	Ordinary-stabilend	0.36	48,000	66,500

Using disk specimens (see section on Corrosion Tests for Welds in Mild Steel), the German Marine[376] found that the corrosion rate of a butt weld in low-alloy structural steel (0.17 C, 1.17 Mn, 0.35 Si, 0.03 P, 0.02 S, 0.36 Cu) made with covered electrodes (deposited metal: 0.09 C, 0.6 Mn, 0.08 Si, 0.03 P, 0.03 S, 0.15 Cu) immersed 20 hrs. in 30% HCl was 8.55 mg. per cm.^{2} per day. The corresponding rates for all-weld metal and unwelded steel disks were 16.6 and 8.67, respectively.

Gibson[115] states that in low-alloy steels with reinforced welds made with covered electrodes, the life of the weld in corrosion is as long as base metal. Johnston and Schramm[116] recommend a coated Cor-Ten electrode for Cor-Ten steel (0.1 C, 0.5–1.5 Cr, 0.1–0.3 Mn, 0.5–1.5 Si, 0.4 Cu) that is to be exposed to extremely severe corrosive conditions. According to Downes,[117] the corrosion resistance of welds in S.A.E. 4130 (0.25–0.35 C, 0.5–0.8 Mn, 0.5–0.8 Cr) is improved by any form of heat treatment above the critical point of the weld metal.

Corrosion of Welds in Stainless Ferrous Alloys

There is considerable literature on the corrosion of welds in stainless ferrous alloys. In fact, there appears to have been more written on the corrosion of unstabilized and stabilized 18–8 welds than on any other subject in the corrosion of welds. For this reason, the corrosion of 18–8 welds is dealt with first in this section of the review, the experimental results being followed by strictly metallurgical investigations and general comments, the last of which are all too numerous. The small amount of information on corrosion of welds in other stainless alloys is summarized in the concluding section.

Table 19—Corrosion of Welded and Unwelded 18–8 in Boiling 67% Nitric Acid. Ellinger and Bibber[118]

Heat Treatment	Total Penetration (Inch/Month) After Five 48-Hr. Runs		
	Plate Metal	All-Weld Metal	V-Butt Weld
None	Less than 0.005	Less than 0.005	Less than 0.005
1 hr. at 650° C.	0.13 to 0.15	Less than 0.005	0.14
1 hr. at 980° C.	Less than 0.005	Less than 0.005	Less than 0.005

Experimental Corrosion Tests of Welded 18–8

1937—Ellinger and Bibber[118]

Corrosion tests of welded (V-butt and all-weld-metal) and unwelded 18–8 were made in three reagents. The specimens were prepared from $^3/_8$-inch plate containing 0.054 C, 18.59 Cr, 9.30 Ni. The electrode ($^3/_{16}$ inch diameter, positive, 23 volts, 170 amps.) contained 0.06 C, 19.26 Cr, 9.07 Ni, and its coating consisted of 43.5% calcium fluoride, 8.2% CaO, 16.5% CO_2, 12.1% SiO_2, 8.8% Al_2O_3, 3.6% MgO, 1.2% Fe_2O_3, 2.6% MnO_2, 0.5% MnO, 2.6% loss at 105° C. Deposited weld metal contained 0.06 C, 18.86 Cr, 9.09 Ni. The all-weld-metal was deposited $^7/_{16}$ inch high on $^3/_8$-inch 18–8 plate with and without water quenching between layers. Specimens $^1/_4$ inch diam., $4^1/_2$ inches long were machined from the all-weld-metal pad. The butt welds were made in four layers with 18–8 backing strip. Specimens were machined from the joint. Welded and unwelded specimens were tested without heat treatment and after air cooling from 650° C. or 980° C. (1 hr. at each temperature).

Reagent 1. 500 ml. of boiling refluxed solution containing 13 gm. per liter $CuSO_4.5H_2O$ and 47 ml./liter H_2SO_4 (sp. gr. 1.84). The electric resistance method of estimating corrosion was adopted, using specimens $^1/_8$ inch diam., $4^1/_2$ inches long. Resistance was measured across the weld, as well as across the weld and heat affected zones. Welded and unwelded specimens were tested without heat treatment and after heat treatment for 1 or 24 hrs. at 650° C. The change in electrical resistance as a result of exposure to Reagent 1 for 100 hrs. (solution renewed after 24 hrs.) was very small (−4.6 to +7.7%) and showed no definite trend. A sensitized unwelded control specimen containing 0.10% C, however, showed an increase in resistance of over 100% after test.

Ellinger and Bibber concluded that Reagent 1 does not cause intergranular corrosion in low-carbon 18–8 even when the grain boundaries contain visible carbides. Since Reagent 1 is known to attack the solid solution depleted by carbide precipitation but not the carbides themselves, it is concluded that the depletion of the solid solution in "sensitized" low carbon 18–8 (0.06% C) is insufficient to cause intergranular attack.

Reagent 2. 500 ml. of boiling HNO_3 (67% by weight, sp. gr. 1.408). Rectangular specimens $1^7/_8$ x $^1/_2$ x $^1/_4$ inch were subjected to five 48-hr. runs, the solution being renewed after each run. The corrosion rate (inches penetration per month) was computed from weight loss for each run. The results are shown in Table 19 and were confirmed by visual observation of the surface of the corroded specimens and by measurements of electric resistance. The authors plotted the initial corrosion rate as zero, and found that the rate increased linearly with time, except in the heavily corroded samples in which the corrosion rate increased rapidly after the second run.

It is known that boiling HNO_3 causes intergranular attack by dissolving the carbides. If the carbides are deposited in quantity at the grain boundaries the acid dissolves the carbide network and causes intergranular attack. Ellinger and Bibber showed that sensitized all-weld metal did not have a microstructure suitable to the development of continuous grain boundary envelopes of carbide. Instead, the carbides in all-weld-metal are distributed uniformly throughout the structure and do not offer a continuous path for intergranular attack. The carbides in the all-weld-metal outlined the areas of ferrite in the "sensitized" specimens. Specimens of all-weld-metal that had been heat-treated first at 980° C., then at 650° C., were not tested.

Reagent 3. 300 ml. of a boiling solution consisting of equal volumes of distilled water and concd. HCl (sp. gr. 1.18) in a covered beaker. Specimens of the same type used in Reagent 2 were subjected to eight 15-minute runs, the solution being renewed after the fourth run. The loss in weight (gm./cm.^{2}) was computed from weight loss after each run. The results are shown in Table 20. It should be observed that the loss of weight, not the corrosion rate, was determined. Since the plot of loss in weight *vs.* time of corrosion was approximately linear for all specimens, the corrosion rate in boiling HCl is constant. There was no intergranular corrosion, the corrosion being of the general and pitting types. The weld metal was particularly subject to corrosion by pitting. The appearance of the butt-welded specimens with or without heat treatment indicated considerable preferential attack of the weld. The difference between the values in Table 20 could not be explained.

Table 20—Corrosion of Welded and Unwelded 18–8 in Boiling 6 *N* Hydrochloric Acid. Ellinger and Bibber[118]

Heat Treatment	Loss in Weight (Gm./Cm.^{2}) After Eight 15-Minute Runs		
	Plate Metal	All-Weld-Metal	V-Butt Weld
None	89	101	99
1 hr. at 650° C.	84	83	85
1 hr. at 980° C.	93	62	90

1935—U. S. Bureau of Standards.[119]

Spot and oxyacetylene welded and soft soldered joints in stabilized and unstabilized 18–8 were subjected to 20% NaCl spray for 500 hours at 35° C. On the whole, joints in stabilized were definitely superior to unstabilized 18–8, particularly soft soldered. (No details.) According to R. H. Aborn (private communication, June 1937), NaCl solution containing iron may attack 18–8.

1935—Schafmeister and Naumann[120]

Gas welds (not heat-treated) in 18–8 containing different amounts of carbon were subjected to boiling acid $CuSO_4$ solution.

0.05% C — 150 hrs. no attack
0.14% C — 35 hrs. almost total destruction
0.22% C — 25 hrs. almost total destruction

Complete protection for at least 150 to 200 hours was afforded to these alloys by adding 0.52 Ti or 1.96 V.

1935—Schoenmaker and Gaymans[121]

Metal arc welds (butt and fillet) in 18–8 were subjected to intermittent corrosion tests in artificial sea

water (3% NaCl plus 0.1% H_2O_2), 10% HCl or 10% H_2SO_4. The welds were not attacked. In acid $CuSO_4$ solution there was some attack.

1935—Bibber[122]

Metal arc welds in 18–8 (as-welded, water quenched 1200° F. (650° C.), water quenched 1800° F. (980° C.)) were subjected to boiling 50% HCl, the weight loss being determined for duration of test up to 2 hours. All-weld-metal was most rapidly attacked in the as-welded condition, least after heat treatment at 1800° F. Joints were most rapidly attacked in the as-welded condition, least after heat treatment at 1200° F. There was very slight advantage in cooling between beads.

Unannealed metal arc welds in 18–8 gave very good results in 4% sea salt spray, 100 hours.

Metal arc welds in 18–8 (0.06 C, 19 Cr, 9.5 Ni) were subjected to HNO_3 (concentration not stated). Specimens as-welded or air cooled from 1800° F. gave good results but specimens heated 1 hr. at 1200° F. and air cooled gave poor results even if the specimen had been quenched after each bead had been deposited. All-weld-metal was unaffected by the treatment at 1200° F. but the welded joints and adjacent base metal were adversely affected.

1935—Meunier[123]

Welded 18–8 (high carbon) became very fragile after treatment in boiling acid $CuSO_4$ but welded low-carbon 18–8 remained ductile. Measurements of the electrode potentials of 18–8 all-weld-metal showed passivity after one day. If the chromium content of the deposit was reduced to 16%, passivity was not observed although the potential was relatively noble (−0.33 volt at 18° C. with respect to calomel electrode in 0.1 *N* KCl).

1935—Musatti and Reggiori[124]

Single pass V oxyacetylene welds in the following 18–8 compositions were tested.

1. unalloyed 18–8, 0.21 C
2. 2.43 Cu, 0.95 Mo
3. 1.46 Mo
4. 1.03 V, 0.15 C
5. 1.02 Ti, 0.17 C
6. 0.5 Cu
7. 1.27 Cu
8. 2.20 W
9. 4.91 W
10. 3.25 Mo, 1.10 V
11. 2.50 Mo, 0.62 Ti

The carbon content was 0.13 to 0.20 in all steels. The specimens were tested for 500 hours in a boiling solution containing 10% $CuSO_4$ and 10% H_2SO_4, in three heat treatments: (1) weld quenched from 1050° C.; (2) plate quenched, then welded; (3) plate annealed at 850° C., then welded. Intercrystalline corrosion was not observed in any of the specimens quenched after welding. Specimens not quenched after welding were severely attacked unless 1% Ti was present. The presence of 1.1% V was partly effective. Specimens of the Ti or V 18–8 were not attacked after 500 hrs. in test solution, after having been held 5 hrs. at 850° C. prior to welding.

1934—Becket and Francks[125]

Gas and arc welds in 18–8 containing 0.1 C with 0.4, 0.7, 0.8 and 1.0% Cb ($^3/_{16}$-inch plate) were subjected to boiling 65% HNO_3 for 240 hrs. Intergranular corrosion, which was pronounced in unalloyed welded 18–8, was completely prevented by adding Cb to the extent of 4 or 5 times the carbon content, the carbon not exceeding 0.10% C.

1934—Fauser[126]

Gas welds in 18–8 were subjected to corrosion in HNO_3. The effect of corrosion on mechanical properties was studied. The effects of cold work and type of flame on corrosion resistance in several concentrations of nitric acid were also investigated. (Original report not available to the reviewers).

1934—Leverick[127]

See Table 21.

Table 21—Corrosion Tests of 18–8 Welds (No Details). Leverick[127]

Reagent	Loss in Gm./Cm.²/Hr. Unwelded	Welded
KI hot, 24 hrs.	Nil	Nil
4% KCl 20° C., 4 weeks	Nil	Nil
20% Acetic Acid, 20° C., 4 weeks	0.0005	0.00055
Saturated NH_4Cl, 20° C., 4 weeks	0.00082	0.00080
10% $MgCl_2$, 20° C., 4 weeks	Nil	Nil
50% Citric Acid, hot, 24 hrs.	Slight	Slight
CS_2, room temp., 4 weeks	Nil	Nil
5% H_2SO_3, room temp., 4 weeks	0.009	0.010
1.42 Sp. Gr., boiling HNO_3, 4 hrs.	Unweighable	Unweighable
10% H_2SO_4, room temp., 1 day	0.0010	0.0012
Conc. HNO_3, 20° C., 30 min.	Unweighable	Unweighable

1934—Miethke and Witt[128]

Welded V2A (0.15 max. C, 0.40–0.65 Mn, 8–9 Ni, 17.5–19.0 Cr), immersed in the following brines for 30 days at −5° C., developed no corrosion.

1. Reinhartin brine, 28° Bé, containing $CaCl_2$ and $MgCl_2$ in 1:3 ratio with protective colloid and buffers.
2. Kühlsoleen 22° Bé (no details).
3. Kühlsoleen Special 19° Bé, about the same as (1) but containing a silicate sediment.
4. Frigisol 22° Bé containing $CaCl_2$ and $MgCl_2$ in 1:3.7 ratio with sodium chromate as anti-corrosive. (Original report not available.)

1934—Mohr, Kramer and Schröter[129]

Welded V2A was resistant to all of a variety of milk products of different acidities. (Original report not available.)

1933—Arrowsmith.[130]

Shot welded 18–8 was dipped in 20% NaCl solution one minute in every quarter of an hour. Welds made with a current duration of over 0.1 sec. rusted rapidly. Welds made with shorter times than 0.1 sec. commenced to rust only after several weeks, the rust being merely superficial. If the blued area of the short-time welds was polished off with metal polish before testing, no rust was observed. (Thickness of sheet not given.)

1933—Jominy and Archer[131]

Plates of stainless steel ($^7/_{64}$ inch thick) were spot welded to a 1-inch carbon steel block. The blocks were welded into stainless steel boxes of the same composition as the liner so that the carbon steel was not exposed. The specimens were 4 x 6 inches and were clamped for 9 to 11 months in sulfite digesters used in the manufacture of paper products. Some of the specimens were hot or cold bent along the 6-inch dimension over a 10-inch radius before testing. Electrolytic action was forestalled by insulating the specimens from the metal parts of the digester. The specimens were photographed before and after test, and were then soaked for 1 month in boiling Na_2CO_3 solution. Some specimens were cleaned by pickling in concd. hot HNO_3 for a few hours. The clean specimens were examined and rated numerically, greatest importance being assigned to surface appearance and pitting, best results given 1, next best 2, etc. The results are shown in Table 22.

Table 22—Corrosion of Spot-Welded Stainless Steel. Jominy and Archer[131]

Material	Rating (Average)
18–8 0.07 C	3.3
18–8 0.18 C	4
16 Cr, 0.08 C	6
20 Cr-10 Ni, 0.05 C	2.5
23 Cr-11 Ni, 0.16 C	2.2

Only the 16 Cr iron liners were badly corroded, but none was corroded through. Hot and cold forming had no effect. Water quenching after heat treatment of the plate metal gave best results. No attack seemed to occur at the spot welds.

employed if the slightest degree of sensitivity must be avoided. Welds in high-carbon 18–8 should always be heat-treated.

1933—Browne[133]

Welded, stabilized 18–8 containing 0.086% C, 0.35% Cb may show 5 to 10 times as much loss in weight in boiling concd. HNO_3 as base metal. In order to be resistant to the acid all welds in 18–8 must be annealed, whether in stabilized or unstabilized material.

1933—Bain, Aborn and Rutherford[134]

Welded unstabilized 18–8 developed bad cracks during a twisting test subsequent to a 260-hr. corrosion test. Stabilized 18–8 (Ti) showed no intergranular cracks after 750 hours in the same reagent.

Table 23—Corrosion of Arc Welds in Stabilized 18–8. Miller[132]

Specimen C	Cr	Ni	Ti	Heat Treatment	Loss of Weight, Gms. After 600 Hrs. Exposure to Sulphite Liquor and Relief Gases	Intergranular Penetration in Mils per 500 Hrs. In Sulphite Digester	In Acid $CuSO_4$
0.07	17.46	9.09	..	Unwelded, 1200° F., 200 hrs.	0.0034 little pitting	1[1]	1200[2]
Ditto				Unwelded, 1200° F., 200 hrs.	0.0325 much pitting	8 to 10[1]	1500[2]
Ditto				Metal-arc weld	+0.0136 (two small pits in heat-affected zone near weld; stained)	0	Trace[3]
Ditto				Metal-arc weld	....	....	1[3]
0.07	18.08	9.12	0.38	Unwelded, 1200° F., 200 hrs.	0.0007 (no stain, little pitting)	Trace[3]	20[2]
Ditto				Unwelded, 1200° F., 200 hrs.	+0.0065 (some stain, no pitting)	0	0
Ditto				Metal-arc weld	+0.0209 (some stain, no pitting)	0	0
Ditto				Metal-arc weld	....	....	0
0.15	19.58	8.40	..	Metal-arc weld	+0.0152 (small pit in heat-affected zone near weld; stained)	Trace[1]	120

[1] Estimated from micrographic examination. [2] Calculated from change in electric resistance. [3] Estimated from torsion test.

Service results of lined digesters were entirely satisfactory.

1933—Miller[132]

Metal arc welds (low-carbon electrode, no details) between a low-carbon plate (0.06 C, 17.3 Cr, 9.2 Ni) and a high-carbon plate (0.14 C, 17.3 Cr, 8.5 Ni), $^3/_8$ inch thick, of which some were water quenched from 2200° F. (1200° C.) after welding, were exposed for 1000 hrs. to operating conditions in a sulfite digester corrosion chamber. The base metals were heat-treated before welding. There was no measurable loss of weight in any of the specimens after test. The heat-treated welds showed no signs of intergranular corrosion, but the high-carbon side of the unheat-treated specimens showed serious intergranular corrosion and carbide precipitation in the grain boundaries.

Similar welded specimens were immersed in a refluxed boiling solution containing 8% H_2SO_4 and 1% $CuSO_4$ for 500 to 600 hours. Neither side of the heat-treated specimens showed any signs of attack.

The results of tests on welded stabilized steels are shown in Table 23. The specimens were 1 inch x 2 inches x $^1/_8$ inch thick. The specimens were first exposed to sulphite, then to acid $CuSO_4$ except the fourth and eighth specimens which were exposed only to the acid $CuSO_4$ test. In the torsion test the welds in unstabilized and stabilized low-carbon 18–8 withstood several full twists before fracture. The weld in high-carbon 18–8 withstood several twists after the digester test, but was brittle after the acid $CuSO_4$ test.

Miller concluded that unstabilized and stabilized 18–8 can be used as-welded without fear of intergranular corrosion by sulphite liquor. The stabilized 18–8 should be

1933—Houdremont and Schafmeister[135]

Gas welds in 18–8 (0.11 to 0.17 C) were subjected to a boiling acid $CuSO_4$ test. Using the appearance of the heat-affected zone in macro-etched specimens as a criterion, it was found that the addition of 5.8 W to base metal and rod was of no effect in preventing intergranular attack. The addition of 4.98 Mo was somewhat effective and the following additions gave perfect protection: 1.30 Ta + Cb; 1.95 V, or 0.52 Ti. In general, the ratio of Ti to C to prevent intergranular corrosion in gas welds is 4:1, corresponding to TiC.

The effect of annealing on the tendency of gas welds in 18–8 (0.14 C) to intergranular corrosion was investigated as a function of time of annealing (600, 750, 900° C.) and depth of affected zone.

Annealing at 600° C. was, of course, very bad. After 100 hrs. at 750° C. or 50 hrs. at 900° C. the weld became stabilized. Graphs are given in the original article.

Intercrystalline corrosion of gas welds in 18–8, it was found, could be largely prevented by a treatment involving cold work followed by annealing at 700° C. Intergranular corrosion could also be prevented (no appreciable attack after 200 hrs. in boiling acid Cu SO_4 solution) by using 18–8 containing alpha phase (0.12 C, 2.13 Si or equivalent).

1933—Pfeil and Jones[136]

Oxyacetylene welded 18–8 (cold rolled, 12 gage, 4 inches x $3^1/_2$ inches) was boiled in acid $CuSO_4$. The effect of the corrosion was measured by change in electric resistance; a 5% change indicated severe attack. The results are shown in Table 24. The specimens for the tensile tests were 1 inch wide, $^1/_{16}$ inch thick. Of the

Table 24—Corrosion of Gas Welds in 18–8. Pfeil and Jones[136]

Chemical Composition						Annealed at 950° C.			Annealed at 1150° C.		
C	Cr	Ni	Mn	Si		Time of Boiling, Days	Tensile Strength, psi	Position of Fracture	Time of Boiling, Days	Tensile Strength, psi	Position of Fracture
0.094	18.59	8.17	0.31	0.44		56	10,100	In weld	18	14,100	Intergranular corroded zone
0.083	18.66	11.94	0.27	0.40		56	5,200	In weld	12	11,400	Intergranular corroded zone
0.097	17.78	14.81	0.20	0.46		56	3,100	In weld	56 at 750° C.	27,400	In weld
0.044	18.81	8.14	0.27	0.30		56	56,000	In weld	56	84,000	In weld
0.184	18.79	7.94	0.36	0.36		21	0	Affected zone	9	0	Intergranular corroded zone
0.05	18.42	14.86	0.25	0.44		56	53,500	In weld	56 at 750° C.	13,000	In weld
0.076	19.09	11.42	0.38	0.35	5.25 Mo	..	....	....	56	108,000	Intergranular corroded zone
0.110	19.20	15.35	0.38	0.63	0.77 Ti	..	....	....	56	52,000	In weld
0.017	22.81	8.65	0.40	2.36		56	105,000	In weld	56	101,000	In weld
0.094	23.15	15.02	0.44	2.55		..	....	....	56	89,000	In weld

17 specimens listed in the table, seven were severely attacked after 3 to 15 days. Of the remainder, two showed attack after 56 days. The alloy, containing 18 Cr, 15 Ni, 0.1 C, was softened at a low temperature before welding.

Specimens of 18–8 gas welded with intersecting (cross) welds at right angles were ground flush, polished and immersed for 6 days in boiling acid $CuSO_4$ solution. Four holes were corroded through at the areas in which the material was heated nearly to the melting point by the first weld and was subsequently heated to 650° C. by the second weld. Cross-welded samples of 18–8 stabilized with Mo or Si or Ti did not withstand the boiling acid $CuSO_4$ test.

1932—Raymond[137]

Oxyacetylene butt welds in 18–8 (0.12 C) 3 x $1^1/_4$ inches, 18 gage were immersed in boiling acid $CuSO_4$ solution (British Air Ministry Test: 10% $CuSO_4$, 10% H_2SO_4). The average loss of weight of 4 specimens after 235 hrs. was 1.8%; a neutral welding flame was used. Specimens of welded stainless steel containing 14 to 15 Cr, 10 to 12 Ni, 0.15 C made with a carburizing flame failed through the weld in a cold-bend test and had no ring when struck after 23 to 34 hrs. in the solution.

1932—Scharschu[138]

Salt spray and atmospheric corrosion tests of welded 18–8 for architectural purposes revealed no difference from unwelded base metal.

1932—Ragsdale[139]

Shot welds in 18–8 in salt spray tests develop a pigmentation due to the rusting of iron particles flashed out of the fused zone. If the weld is washed in dilute HNO_3 before testing the rusting does not occur. Salt water immersion for 14 months developed no weakness, however, in shot welds that had not been washed.

1930—Houdremont[140]

Gas welds in 0.55 inch stabilized 18–8 showed no intergranular corrosion after 2500 hrs. in boiling acid $CuSO_4$ solution.

1930—Pilling[141]

Welded specimens of 18 Cr–8 Ni and 18 Cr–14 Ni containing 0.14 and 0.23 C, respectively, were subjected to prolonged corrosion in H_2SO_4 at room temperature. The former developed severe intergranular corrosion, the latter none.

1926—Chemisch-Technische Reichsanstalt[142]

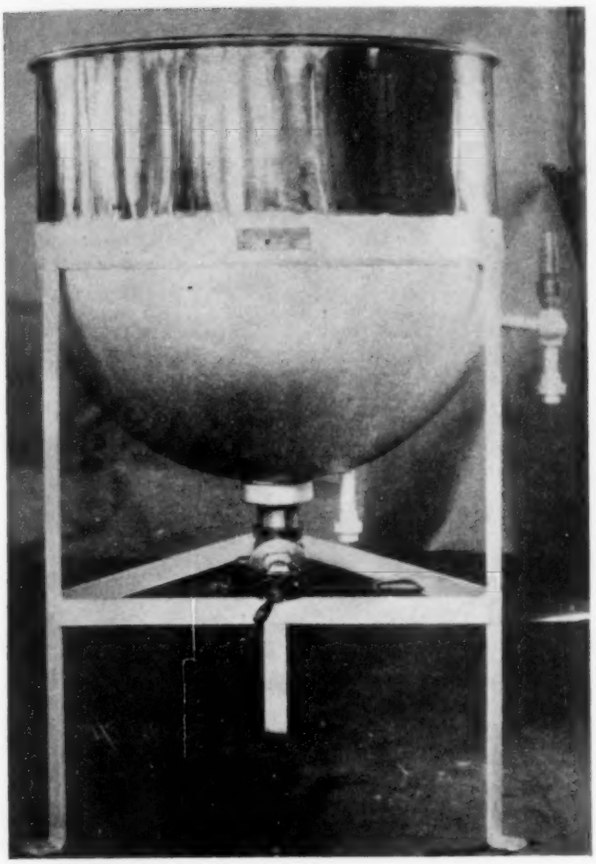
Welded specimens of V_2A (18–8) sheet, heat-treated and not heat-treated after welding, were tested in 3% NaCl solution and aqueous $MgCl_2$ solution (no details). After a short time rust developed in the junction zone and weld of the unheat-treated samples. The slight traces of rust on the heat-treated samples were attributed to the rough surface of the weld. The poor behavior of the unheat-treated samples was ascribed, in a general way, to differences in microstructure and electrolytic potential (no details).

Metallurgy

The metallurgical aspects of the corrosion of welded 18–8 center around the intergranular precipitation of carbides in the heat-affected zone of welds. The intergranular carbides produce the effect known as intergranular corrosion which is characterized, as Rollason[143] points out, by the development of brittle cracks in bent corroded specimens. (The term "weld decay" is misleading. The use of the term as a substitute for "intergranular corrosion" is not correct.)

The presence and magnitude of the effect depends on the composition and heat treatment of the steel, the process of welding, and the corrosive agent involved. The phenomenon of intergranular corrosion in welded joints is said to have been first noted by Monypenny,[144] who states that the length of time in the temperature range 600 to 700° C. required to produce sensitivity to boiling acid $CuSO_4$ solution is the basis for weldability. Unstabilized 18–8 requires less than 1 minute at 600–700° C. to render it sensitive to intergranular corrosion in the solution. Houdremont[140] (1930) stated clearly that intergranular corrosion is caused by the precipitation of carbides, a view that has been unconvincingly attacked by Payson,[145] who ascribed the cause to incipient carbide precipitation.

The nature of intergranular corrosion in welded plate was clarified by the experiments of Strauss and Schafmeister.[146] Oxyacetylene butt welds in 0.08-inch 18–8 that had been quenched from several temperatures between 800 and 1150° C., were boiled for 48 hrs. in a solution of 10 parts of water to 1 part H_2SO_4 to which 10% $CuSO_4$ was added. No intercrystalline corrosion was observed in low-carbon 18–8. In welds in high-carbon 18–8 the intergranular corrosion was absent in plates that had been quenched from 800 to 900° C. prior to welding, but became increasingly pronounced as the quenching temperature was increased from 950 to 1150° C. The explanation of the results is that the material quenched from below 950° C. is fine grained,



Rezistal 2—A Steam Jacketed Kettle or Cooking Soup

and the carbides are not all in solution but exist as relatively large globules scattered within the grains, not at the grain boundaries. Mill practice to obtain the stabilized structure consists of heavy reductions at low temperatures followed by short-time annealing at 800 to 900° C. Upon welding, the carbide precipitated in the heat-affected zone deposits on the existing nuclei, and there is no finely divided intergranular carbide. On the other hand, upon welding 18–8 water quenched from about 1100° C. and having a rather large grained structure free from carbides, a zone of fine intercrystalline carbides is formed, which gives rise to intergranular corrosion in some corrosive media.

The temperature at which intergranular corrosion occurs in unstabilized 18–8 as a function of the time at that temperature has been determined by Rollaston.[147] Material that has been reduced 75% by cold rolling is subject to intergranular corrosion at lower temperatures than material water quenched from 1100° C., but the cold-rolled material is more quickly stabilized by prolonged heating in the sensitive range, stabilization occurring by growth of the carbide particles beyond the size which induces sensitivity and by equalization of composition in the otherwise sensitized regions.

Stabilizing elements, such as Cb, Ti and Si, may act in two ways to prevent intergranular corrosion. Carbide-forming elements prevent local dilution of the sensitized region in chromium. Ferrite-forming elements restrict carbide precipitation to the ferrite islands; the grain boundaries are not sensitized. A decrease of carbon content is effective in reducing intergranular corrosion by reducing the amount of carbide available for precipitation. Rollaston also states that shrinkage stresses perhaps highly localized around the precipitated particles, may hasten intergranular corrosion. If the weld is water quenched from about 1100° C. all carbides are redissolved and held harmless in solid solution.

The microstructure of welds in 18–8 is discussed by the authors already cited in this section and by Bull.[148] Ellinger[149] shows that his oxalic acid reagent is useful for detecting carbide precipitation in welds, and Van den Bosch and Vialle[150] recommend their perchloric acid reagent for the same purpose. Murakami's reagent (potassium ferricyanide) is also useful.

According to the U. S. Bureau of Standards,[119] neither spot welding nor soft soldering causes intergranular corrosion in stabilized or unstabilized 18–8. Some carbides were detected at the edge of oxyacetylene welds in stabilized 18–8 but not at any appreciable distance from the weld. Gas welding of unstabilized 18–8 caused carbide precipitation at a considerable distance from the weld.

General Observations—Carbon Content

The effect of increasing carbon content is to increase the tendency to intergranular corrosion, as Hatfield[151] and others state. The carbon content of base metal and filler rod should be below 0.07%, according to Leroy and Bonnot.[88] Harris[152] and Wachowitz[153] recommend the use of low-carbon filler rods. Rollaston,[147] however, considers that lowering the carbon content is not generally practicable, probably from the cost standpoint, and Johnston[154] states that welds in 18–8 with 0.05 C have about the same corrosion resistance as welds with 0.10 C. Hodge,[21] however, definitely states that high-carbon 18–8 welds are inferior to low-carbon in resistance to intergranular corrosion. The high-carbon welds must be quenched from higher temperatures than low-carbon to obtain insensitivity to intergranular corrosion.

All[155] are agreed that carbon pick-up from a carburizing flame is damaging to corrosion resistance of 18–8 welds. Priestley[156] and Pratt[157] state that the carbon pick-up (0.02–0.04 C) can be counterbalanced by using a filler rod containing Cb.

Alloying Elements

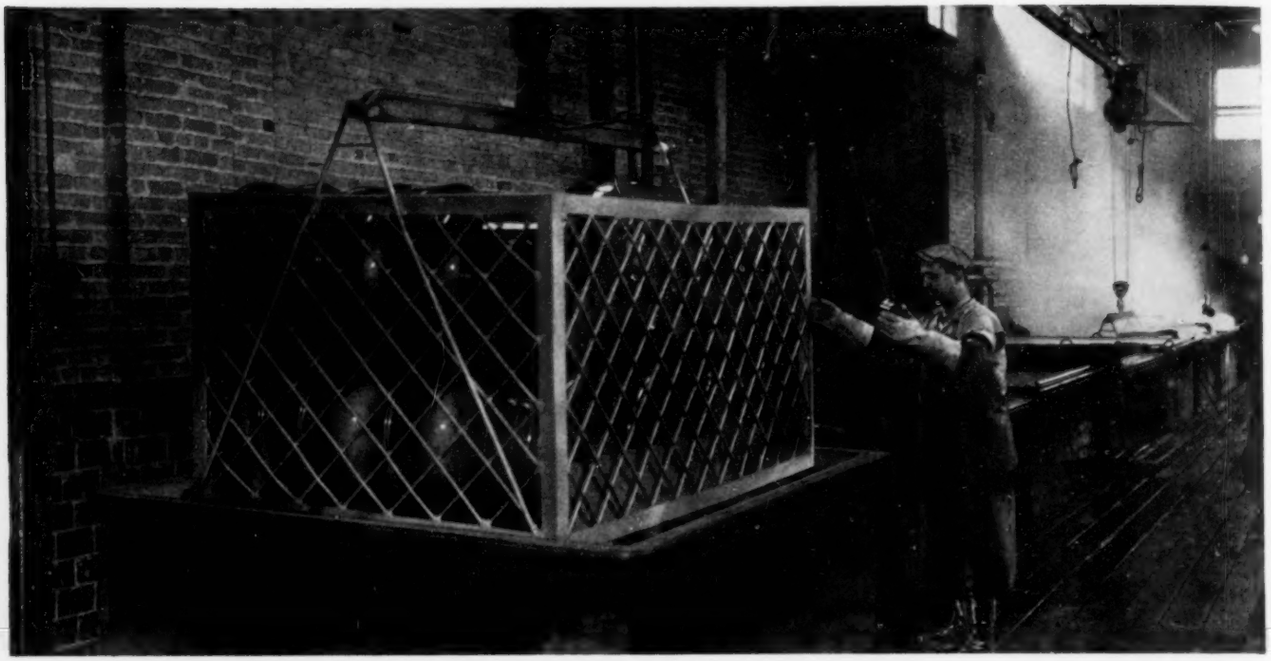
Table 25 contains recommendations of suitable alloying elements to prevent intergranular corrosion.

Table 25—Alloying Elements to Prevent Weld Decay in 18–8

Element	Authorities
W	155, 158
Ti	21, 88, 147, 155, 158, 159, 160
Ta	88, 159, 161, 162, 163
Cb	88, 156, 164
V	159
Si	147
Mo	147

It should be borne in mind that the table is based on statements referring specifically to intergranular corrosion in 18–8 alloys.

An alloy known as Weldanka (18–8) is recommended by Thompson.[165] Pilling[166] recommends an increase in the nickel content. Hermann[167] states that welds in austenitic Cr-Ni steels (7–15 Ni, 16–25 Cr, 0.3–2.5 Si, 0.05–0.15 C) are as resistant to thermal and chemical attack as base metal. The titanium content, according to Harris[152] should be: Ti = 6 × C + 0.02, although Houdremont and Schafmeister[135] found that Ti = 4 × C was adequate to resist intergranular corrosion in boiling



One of Six All Gas-Welded Monel Pickling Crates for Hollow Ware Built of Hot Rolled Monel Angles, Flats and Rounds. Each Crate Weighs Approximately 744 Lb., Which Weight Is Considerably Less Than Previous Crates of Riveted Construction

acid $CuSO_4$ solution. Pratt[157] states that intergranular corrosion even at intersecting welds, as indicated by the HNO_3-HF test (no details), is prevented by using 18–8 containing 0.67 Cb in conjunction with 0.06 C. Becket and Francks[125] state: Cb = 4 to 5 × C (carbon being not over 0.10%). Monypenny[406] found that when titanium-treated 18–8 is welded, the weld may contain only 0.1% Ti without danger of intergranular corrosion, although the electrode may have contained 1% Ti. A high-silicon electrode deposits weld metal which is not subject to intergranular corrosion.

According to Bull,[168] intergranular corrosion is prevented by adding 0.6 Ti or V, or 2 Si, or 0.07 C or less, or 1.9 W. The tungsten addition is said to be obsolete commercially. Hignett[169] doubted whether Cb eliminates intergranular corrosion at intersecting oxyacetylene welds, but Kinzel assured him that a correct Cb-containing rod can be relied upon completely to prevent the corrosion. A German steel works[170] has patented the following rod for eliminating intergranular corrosion in 18–8: 0.15 C, 0.5 Si, 0.5 Mn, 15.0 Cr, 13 Ni, 4.5 Ta. Hatfield[404] states that 18–8 with 0.12 C, 0.63 W, 0.69 Ti is proof against intergranular corrosion.

Heat Treatment

As indicated in the section on experimental results correct heat treatment raises the corrosion resistance of welds in unstabilized 18–8, and to a very minor extent, in stabilized 18–8. Bauer, Kröhnke and Masing[29] in their text-book on corrosion (1936) advise heat treatment of 18–8 welds wherever possible. Scharschu[138,171] states that heat treatment is necessary to develop maximum corrosion resistance, but is unnecessary for welded architectural work. Heat treatment is necessary for welded 18–8 sulphite pulp apparatus, according to Van Dyke,[172] but is not required for restaurant equipment. After they are welded, 18–8 gasoline tanks are annealed and passivated in 20% HNO_3, as Rock[173] states. The heat treating temperature for developing maximum corrosion resistance in welded 18–8 is 1900–2100° F. (1040 to 1150° C.), according to Hodge,[21] depending on carbon content. Moritz[174] also gives 1050–1150° C. as the range for water quenching but a soak at 900° C. is equally effective in his experience. Rollason[147] is not in favor of a low temperature anneal following cold work, especially for welds containing 0.15 to 0.20 C, because maximum corrosion resistance is not developed.

Technique

Aside from the factors of technique which have already been discussed, principally torch adjustment, it is essential that welds in 18–8 be given a good finish in order to develop good corrosion resistance. Rovzev[175] recommends that welds be pickled to remove scale after heat treatment, and then passivated with 20% HNO_3. Slag and burred edges should be removed [154] and all flux thoroughly cleaned off.[88,176] Holler[177] points out that hammering does not improve the corrosion resistance of welds in 18–8, that slag inclusions cause local corrosion due to electrolytic action, and that grinding is desirable. Detailed instructions for grinding and polishing welds in 18–8 for best appearance and corrosion resistance are given by Grismer.[178] Two types of corrosion occur in blowholes and cracks in 18–8 welds, Rollason[155] states: (1) hydrogen evolution type; (2) oxygen concentration cell type. Tay[179] maintains that welds should not be made in corners of 18–8 restaurant equipment where they are inaccessible to grinding because food lodges in the crannies and ferments.

Miscellaneous

The application of 18–8 surfaces welded to steel castings and machinery and to water wheel buckets in order to increase the corrosion and wear resistance is described by Newell[180] and Gaby,[181] among others. The welding of 18–8 liners to steel fittings is discussed by Hoffman.[182] Hurme[183] points out that, if there is dilution of the 18–8 layer during the welding of stainless clad, there is, of course, a decrease in corrosion resistance. Tichodeev[184] found that joints in stainless-clad soft steel butt welded with stainless electrodes so that only the stainless coat-

ing was actually welded, gave satisfactory results in contact with canned food.

DeGanahl[185] states that spot-welded 18–8 always commences to corrode at the spot. As Nelson[395] points out, there is susceptibility to corrosion when the corona of spot welds extends to the surface of the sheet. Arrowsmith,[186] however, found that spot welds did or did not corrode depending on the composition of the 18–8, and Geschelin,[187] who describes the corona effect in corroded spot welds in 18–8, shows that the welding time in the Budd shot weld process is too short to develop intergranular corrosion.

Hignett[188] believes that intergranular corrosion is a particularly damaging type of corrosion under conditions of fatigue loading. Relatively few examples of the service behavior of welded 18–8, beyond those quoted earlier in the report, have been described. As early as 1929, Holler[189] accurately described the intercrystalline corrosion of an oxyacetylene welded stainless steel container after long service, but he did not then know the explanation.

Moses[11] and Hatfield[373] described similar failures, but Hatfield (1929) attributed failure to stress-corrosion cracking, shrinkage supplying the stress.

Scharschu[190] refers to a welded steam-jacketed kettle in high-carbon 18–8 that gave excellent service for 2 years with no signs of corrosion. No intergranular corrosion was noted by Thompson and Van Duzer[374] in fusion-welded 18–8 high-pressure steam pipe joints (no details).

Proposed Corrosion Tests for Welded 18–8

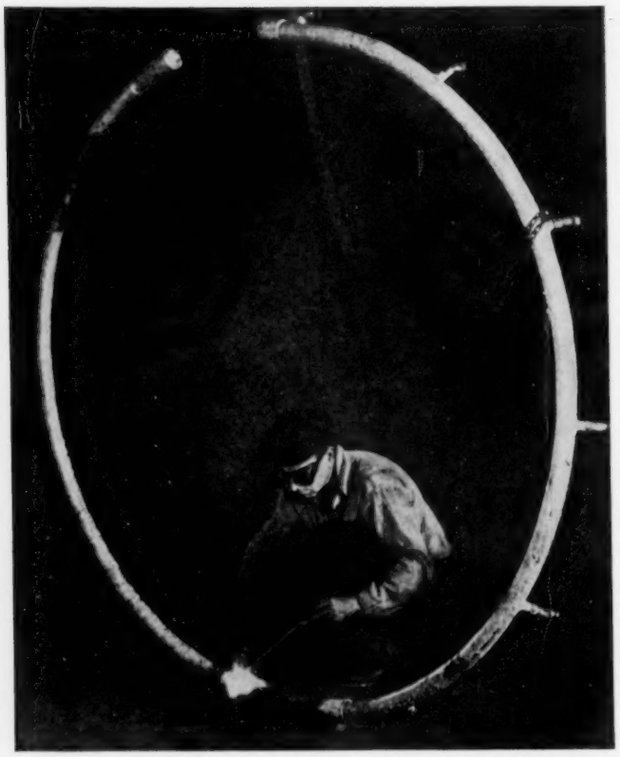
A number of proposals have been made for testing the corrosion resistance of welded 18–8. The British Air Ministry[166] specified that bend test specimens be immersed for 72 hours in a refluxed boiling solution containing 98 gm. H_2SO_4 and 111 gm. $CuSO_4$, made up to 1 liter. After the test the specimens must withstand 180° bend without cracking.

The A. S. M. E. Boiler Code Committee[372] decided that free-bend test specimens for welded unfired pressure vessels in 18–8 (0.07 C, max.; stabilized by columbium or titanium) must be subjected to a test to determine susceptibility to embrittlement. The welded specimen, about 6 inches long, is polished and immersed for 48 hrs. in a boiling solution containing 47 ml. concd. H_2SO_4 and 13 grams of crystalline copper sulfate per liter. After removal the specimens are subjected to the standard free-bend test. "The elongation on the outside fibers shall not be less than 20% at which there shall be no evidence of fissuring."

The U. S. Navy sometimes specify that 18–8 welds must pass acid corrosion and salt spray tests. The acid test consists of immersion for 96 hours in 67% HNO_3; the salt spray test involves exposure for 20 hrs. to 20% NaCl spray. Ellinger and Bibber[118] do not regard boiling 6*N* HCl as a suitable reagent for testing welds in low-carbon 18–8 (about 0.06% C).

Poste[191] recommended that the tentative standard A. S. T. M. corrosion tests for unwelded 18–8 be applied to welds, but the A. S. T. M. Committee on Iron-Chromium Alloys have given up attempting to formulate specifications for corrosion testing welds in 18–8. Whitney[192] advocates the Huey test (up to 48 hrs. in boiling 65% HNO_3) for corrosion resistance of 18–8 welds, and the acid $CuSO_4$ test (72 hours in a boiling solution containing 3% $CuSO_4$ and 10% H_2SO_4 followed by a bend test) for detecting sensitivity to intergranular corrosion. Browne[123] considers that the boiling concn. HNO_3 test is more severe than the acid $CuSO_4$ test, but he does not show that the same property is determined by both

Abrasion-Resistance and Corrosion-Resistance Is Inherent in Hard-Faced Valve Disks for Handling High-Sulphur Crudes and Other Corrosive Fluids



Both Steel- and Bronze-Welding Saved This Brine Cooling Coil Which Had Become Badly Corroded in Spots

tests. It may be mentioned that the boiling acid $CuSO_4$ test mentioned in this report is variously referred to as the Hatfield, Strauss or Krupp test. Priority for applying the test to welds does not seem to have been established by any one. An intersecting or cross-weld test is suggested by Pfeil and Jones.[136] Bibber[122] proposes several quick corrosion tests. A cross weld or plain weld is immersed for 24 hours in a solution containing 10% HNO_3 and 2%HF at 140° F. (60° C.). The concd. HNO_3 test is also favored for a quick test. According to Bibber,[122] good weld metal or joints should show no increase in electric resistance after boiling in acid $CuSO_4$ solution for 72 hours. In addition to intergranular and chemical solution corrosion, 18–8 welds are also subject to corrosion of the contact and concentration cell type but no routine tests have yet been developed, Bibber stated in 1935.

Other Stainless Ferrous Alloys

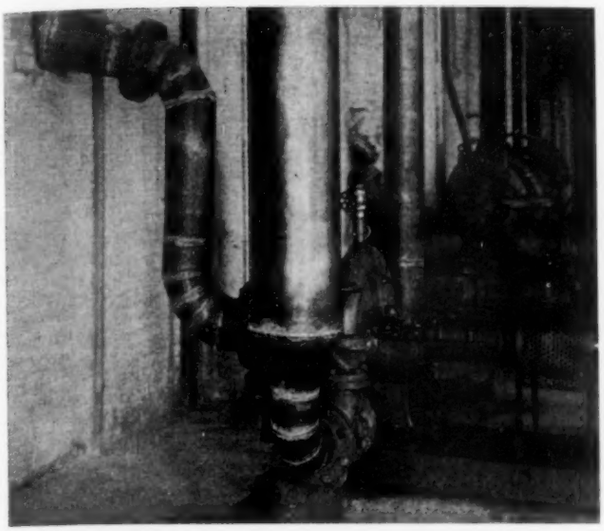
Plain Chromium Stainless Alloys.—According to Dixon (Thum[193]), the corrosion resistance of properly heat-treated 4–6 Cr weld metal is the same as base metal, which is also the experience of Hodge.[194] The same statement is made by Hodge concerning the 12–16 Cr alloys, experimental sulphite corrosion tests on which have been reported by Jominy and Archer[131] (see Experimental Section: 18–8 Alloys). Weinman,[195] however, states that atomic hydrogen welds in the 13–15 Cr alloys do not have so good corrosion resistance as base metal, and Moses[11] found that 16% Cr iron, as-welded, corroded in the heat-affected zone, a condition that was cured by heat treatment. It is Miller's[196] belief that oxyacetylene welds in 13% Cr alloy must be heat-treated if the carbon content is 0.3% but need no heat treatment if the carbon is 0.1%, provided the flame is very slightly carburizing. According to Hengler,[197] intergranular corrosion in ferritic plain-chromium stainless alloy is caused by carbide precipitation in the heat-affected zone during welding. The precipitation lowers corrosion resistance of welded joints only in those solutions to which the base metal itself is only moderately resistant. A filler rod containing 0.5 C, 0.8 Si, 0.3 Mn, 20.0 Cr, 6.5 Ta has been patented by a German firm[170] to prevent intergranular corrosion in plain-chromium stainless alloys. The use of 18–8 filler metal is recommended by Thum,[193] provided galvanic action does not occur, but Hatfield[151] states that an austenitic rod for plain-Cr stainless is of doubtful value in corrosion. Moses[11] found that although heat-treated 16–18 Cr welds in 16–18 Cr steel or 18–8 welds in 18–8 plate showed no corrosion in HNO_3 test, an 18–8 weld in 16–18 Cr developed corrosion in plate metal adjacent to weld, as-welded. If the plate were properly heat-treated (1450° F. furnace cool) the weld was heavily corroded in HNO_3. Welding rods containing nickel lower the resistance to sulphur compounds, HNO_3 and oxygen at high temperatures, according to an anonymous writer.[55] The salt spray and boiling acid $CuSO_4$ solution are used for testing the corrosion resistance of welds in high-Cr stainless iron, Thum[193] states. Reavell[58] reports satisfactory results from a welded and heat-treated plain-Cr stainless alloy vat for citric acid over a period of 5 to 6 years.

Accelerated cavitation tests were made by Mousson[407] using fresh water and specimens $4 \times 1\frac{1}{4} \times \frac{1}{4}$ inches, consisting of wrought iron plates covered with two layers of arc-deposited plain-carbon and low-alloy steels, 12 to 28% chromium irons, 18–8, and 29–9, or oxyacetylene-deposited bronzes and stellite. The weld deposits gave about the same results as rolled material of the same analysis, and lost only about 5% as much by volume through cavitation after 16 hrs. at 20° C. as sprayed deposits. Of the materials tested, the chromium irons were least attacked by cavitation.

Special Stainless Alloys

Welds in 29 Cr–9 Ni castings, Wilcox[198] found, could be bent 180° without cracking after having been boiled

Yeast Buckets of Welded Stainless Steel Last Longer Because of Their Corrosion Resistance



Bronze-Welded Copper Piping for Carrying Pulp from Beaters to Paper Machines and Bleachers Has Eliminated Pulp Discoloration

for 72 hrs. in acid $CuSO_4$ solution. Schuler[199] notes that gas holes have a bad effect on the corrosion resistance of coated electrode welds in 25 Cr–12 Ni alloy. W. B. Miller[200] states that welds in Hastelloy A (20 Fe, 60–58 Ni, 20 Mo, 0–2 Mn) have satisfactory corrosion resistance, and Hanel[159] observed no intergranular corrosion in welded or brazed joints in an alloy containing 80 Ni, 14 Cr, 6 Fe, when subjected to $CaCl_2$ solution. Welded 40–60 Ni, 15–25 Cr, 5 Mo, rem. Fe developed no attack at the weld in HCl. (See section on Corrosion of Nickel Welds). Welds in Duriron (15 Si, 0.8 C) have a higher corrosion resistance than base metal, according to Schenck[202] and Tull,[203] but Tungay[58] writes that, using high-Si filler rod and any welding process, the weld is invariably low in silicon and consequently has poor corrosion resistance.

Corrosion of Welds in Aluminum and Its Alloys

The present review consists of a chronological summary of experimental corrosion results on welded aluminum followed by general observations on corrosion as affecting welding technique. The summary on welded aluminum is followed by a similar summary on welded aluminum alloys. The composition of the alloys is that given by either the investigator or "Engineering Alloys."

Experimental Corrosion Tests on Welded Aluminum

1937—Hoglund[201]

Tensile tests on spot welds in 2 S $^1/_2$ H, after exposure for one year at Point Judith, R. I., where salt spray is deposited on the specimens in stormy weather, revealed no decrease in strength. After two years exposure there was no evidence that the strength had changed; actual tests were to have been made in June 1937. Visual examination of spot-welded panels in 2 S and 3 S subjected to 6300 tidal immersions ($10^1/_2$ summer months) in brackish Hudson River tide water (containing appreciable quantities of industrial waste), at Edgewater showed no preferential attack of the welds. The panels were so exposed that at hourly intervals the lower portion was immersed continually in water, the middle portion was immersed alternately, and the top was exposed to air.

1937—Buchholz[394]

Welds were made in annealed 99.5% aluminum, 1 inch thick, with the following commercial rods: unalloyed aluminum oxyacetylene welding rods and electrodes; aluminum with 5% Si, oxyacetylene; aluminum with 0.2% Ti, oxyacetylene. The oxyacetylene welds were X, made by the two-torch vertical method. Arc welds were V, reverse welded, 270 amps. D.C., preheated (no details) four layers, first layer 0.32-inch electrode, other layers 0.39-inch electrodes. After 63 days in the DVL test (agitated solution containing 3% NaCl, 0.1% H_2O_2) using machined specimens, the welded joints containing silicon were heavily attacked. The welds made with commercial aluminum rods or electrodes had about the same corrosion resistance as base metal. The welds containing titanium were inferior, but only slightly. Slag or flux inclusions greatly lowered the corrosion resistance of welds of any type (no details).

1936—Hoglund[204]

Spot welds in Alcoa 2 S (99.0–99.4 Al, 0.6–1.0 Fe, Si, Cu) and 3 S (98 Al, 1.25 Mn, 0.5 Fe). Laboratory salt spray tests and exposure at Point Judith, R. I., where salt spray is deposited on the specimens in stormy weather produced no significant loss after 1 year. No details of specimens or experimental conditions.

1936—Bürgel[205]

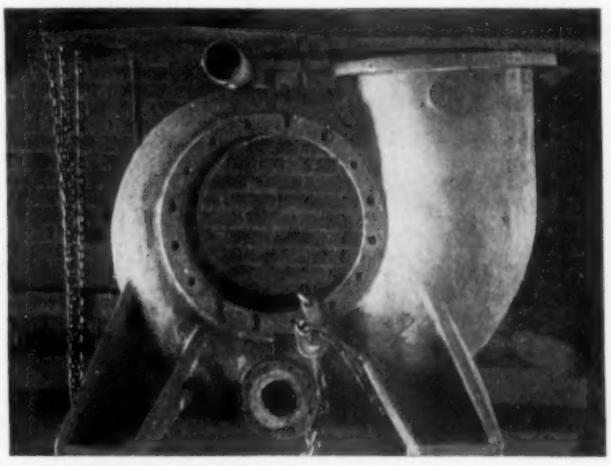
Gas welds made in the usual way with flux in aluminum (98–99% Al), pickled, then neutralized with 1% HNO_3. The reagents were 27 in number, comprising 7 acids, 10 dye reagents, 3 fuel oils, acetone, and lubricating oil, all at 20° C. for 100 days. Quantitative results are not given. In all reagents except 1 *N* HCl weld and base metal behaved about the same. Annealing was of no advantage. In HCl the heat-affected zone was preferentially attacked on account of coarse grain structure.

1936—Gelman and Kazen[206]

Gas and arc welds in 0.12-inch sheets of Al containing 0.93% Cu were tested in 95, 75, 55, 40 and 20% HNO_3 at room temperature, 25 and 60° C., with and without stirring, elongation in tension before and after test being measured. The gas welds were more corrosion resistant than the arc. Corrosion was more rapid in 55% HNO_3 than in 75 or 95% HNO_3, but it was considered that welded apparatus can be used up to 60° C. in 55 to 95% HNO_3 with safety.

1935—Zechnovitzer[207]

Carbon arc and metal arc (0.20-inch aluminum electrodes) welds in rolled aluminum (0.21 Si, 0.75 Fe, 0.34



Cast-Iron Welding Reclaimed the Corroded Throat of This Rotary Impeller Pump for Handling Hot Liquor in a Paper Mill

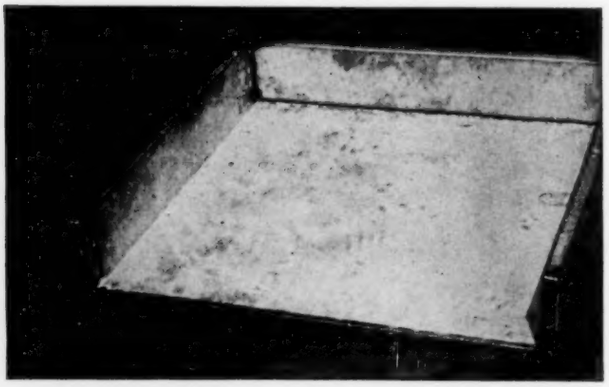
Cu) were immersed in 5% HCl, 5% NaOH, NH_4OH, H_2SO_4, 0.58% NaCl, sea water, and in a solution containing 1% NaCl, 1% H_2O_2. The loss in weight, as well as tensile and bend properties, showed that welded specimens had greater resistance to most reagents than unwelded. The welded specimens were most heavily attacked in the heat-affected zone, the carbon arc welds showing better resistance than metal arc, on account of the higher welding temperatures in carbon arc. In HCl and NaOH (especially the latter) the weld was more attacked than base metal. In NaCl the weld had the same resistance as base metal.

1935—Röhrig and Nicolini[208]

Gas-welded pure hard rolled Al (0.04 inch thick) was subjected to tidewater corrosion in North Sea water for 18 months. The loss in strength of the welds as a result of corrosion was inappreciable. (Original report not available.)

1935—Haarich[209]

Butt welds in cold rolled Al plate (0.16 inch thick, 99.35% Al) were made by the following processes: A.C. and D.C. metal arc, Arcogen, oxyacetylene and oxyhydrogen. The welds were immersed for 30 days in 10 vol. % H_2SO_4 or 10 vol. % acetic acid. The D.C. arc welds were made with 0.16 in coated electrodes. All welds were made in the horizontal position on preheated plates. The arc welds were tacked, the gas welds were not. Some of the welds were cold hammered with a compressed-air hammer. Some of the hammered welds were heated in a salt bath for 30 mins. at 400° C. and quenched in cold water. The decrease in weight of the welded joint after 30 days in comparison with unwelded plate corroded for the same length of time is expressed as % and is shown in Table 26.



This Stainless Steel Scale Tray, Used for Weighing Fats and Other By-Products in the Meat Packing Process, Is Completely Fabricated by Welding

Table 26—Corrosion of Welded Aluminum. Haarich[209]

Each figure is the ratio, in per cent, between the decrease in weight of the welded specimen and the decrease in weight of the unwelded sample.

	10 Vol. % Acetic Acid			10 Vol. % H_2SO_4		
Method	As-Welded	Cold Hammered	Cold Hammered and Annealed	As-Welded	Cold Hammered	Cold Hammered and Annealed
Oxyhydrogen	50	70	40	140	160	120
Ditto, machined	60	..	50	180	...	170
Oxyacetylene	75	90	85	160	230	190
Arcogen	..	..	..	150	240	150
D.C. Arc	..	..	..	180	250	120
A.C. Arc	..	..	..	220	250	...

The machined oxyhydrogen welds were machined flush with plate; the other welds were unmachined. The results show that all welds were worse than unwelded in H_2SO_4. Cold hammering or machining decreased the corrosion resistance. In acetic acid the oxyhydrogen and oxyacetylene welds were better than unwelded. Corrosion was relatively greater after 15 days than after 30 days because a protective film was gradually formed on the weld metal. The recrystallized zone near the weld was more corrosion resistant than base metal.

Haarich also determined the distribution of elongation during tensile tests of the corroded welded specimens, and the variation of hardness across the corroded welds. The maximum percentage elongation (measured locally on an original length of about 0.16 inch) of the corroded welds after 30 days corrosion is shown in Table 27. The elongation, especially of the cold hammered welds, was reduced considerably by corrosion in H_2SO_4. Annealing after cold hammering increased the elongation above the as-welded value for D.C. arc welds. The corroded welds (20 to 30 Brinell) were a little softer than uncorroded, about 5 Brinell units, except in the specimens which were annealed after cold hammering in which corrosion produced practically no change in hardness.

Table 27—Ductility (Elongation %) of Corroded Aluminum Welds. Haarich[209]

Method	As-Welded (uncorroded)	H_2SO_4	Acetic Acid
Oxyhydrogen as-welded	78	52	91
Oxyhydrogen cold hammered	83	58	98
Oxyhydrogen cold hammered and annealed	99	38	81
D.C. Arc as-welded	85	65	..
D.C. Arc cold hammered	82	40	..
D.C. Arc cold hammered and annealed	125	100	..

1935—Meunier[123]

Covered electrode welds in aluminum plate. Corrosion was negligible after 5 days at 18° C. in a solution containing 3% NaCl, 0.1% H_2O_2, and after 8 days in concd. HNO_3. The emf. of the welds in 0.1 *N* KCl with respect to calomel electrode showed that weld was more noble than base metal.

1935—Holler and Maier[210]

Gas welds (butt, pure Al filler rod) in 0.20-inch plate. One plate of the weld was Al, the other was Fe, Cu or Pb. Reagents were atmosphere, tap water containing 170 mg. chloride per liter, tap water plus 3% NaCl; total immersion up to 2 months. Corrosive effect was measured by loss of tensile strength. Tap water and salt solution had no effect on the Al-Fe joint, but accelerated attack occurred on the iron plate merely by contact with Al. The junction zone of the weld corroded more than the weld itself. The same was true for the Al-Cu and Al-Pb joints, but the strength of the Al-Pb weld had decreased 50 to 75% after 4 weeks in the salt solution.

1933—Anastasiadis[211]

Welds made with fluxed Al electrodes (positive) in Al plate were immersed in 2% HCl with slow mechanical agitation of the bath. Base metal, weld metal and welded joints were tested up to 60 hours. After 48 hours the loss of weight of the specimens was:

Unwelded plate	315 Mg./Cm.^{2} loss
All-weld-metal	330 Mg./Cm.^{2} loss
Hot forged joint	340 Mg./Cm.^{2} loss
Cold hammered joint	355 to 370 Mg./Cm.^{2} loss

The decrease in tensile properties is shown in Table 28.

Table 28—Decrease in Tensile Properties of Aluminum Caused by Welding. Anastasiadis[211]

	Unwelded Plate		All-Weld-Metal		Welded Joint	
	7 Hrs.	48 Hrs.	7 Hrs.	48 Hrs.	7 Hrs.	48 Hrs.
Tensile strength, psi	20,700	10,000	15,700	2100	13,500	1100
Elongation, %	40	17	40	14	24	10

1933—Weiss[212]

Gas welds, pure aluminum filler rod, made with "Universal" flux (no details), showed negligible decrease in tensile strength after 10 hrs. in boiling water. Total immersion in 3% NaCl for 5½ months at 18 to 25° C., the solution being renewed from time to time, of welds machined to sheet thickness before testing, but not cleaned free from flux, caused pitting type of corrosion. The tensile strength of the welded specimens was the same as the unwelded after test, although both had lost strength as a result of corrosion.

1933—Anonymous[213]

Gas and metal arc (Arcos Alumend electrode) welds in pure aluminum tested in 30° Bé. H_2SO_4, and also 1 to 5 days in a solution containing 3% NaCl plus 0.1% H_2O_2. Hammered machined welds and base metal in the latter reagent corroded uniformly, but in the former the gas welds corroded less uniformly (pitting) than arc welds.

1930—Holler[214]

Hammer welded and oxyacetylene welded aluminum plates immersed in stoppered flasks containing 10 and 96% acetic acid, concd. H_2SO_4, 1.10 sp. gr. H_2SO_4, 1.50 sp. Gr. H_2SO_4, and HNO_3 (same gravities as H_2SO_4). The results are presented in graphical form and show that the welds had good corrosion resistance.

Quenching a weld in 99% Al held the silicon in solid solution and decreased the corrosion rate up to 40%, depending on the electrolyte. Cold hammered welds corroded 40% faster than as-welded, the weld being preferentially attacked.

1930—Wyss[215]

Gas welds in pure Al plate (0.08 inch thick) made with Reuss flux (a water solution of alkalies). In one series the flux was used fresh; in the other the flux was exposed 7 weeks to the air. Total immersion tests for 2½ months in 3% NaCl showed no greater attack in weld than in plate.

1929—Anonymous[216]

Welded Al plates (0.12 inch thick, 5 inch long, 2⅜ inches wide, hot rolled 300 to 400° C.) in North sea water, Table 29, renewed daily, contained in glass beakers. Specimens were totally immersed for about 10 months (Feb.–Nov. 1925) at 7 to 13° C. Results are reported as weight loss (Mg./Cm.^{2}) and appearance of specimen. The specimens were tested as welded and with the following coatings: (1) chromate, (2) electrolytic treatment, (3) sprayed with lead. The welded specimens were not heat-treated. There was slight preferential corrosion of the weld in the uncoated specimens, but none in the coated.

Table 29—Corrosion of Welded Aluminum in North Sea Water. Anon.[216]

		Coating		
	Uncoated	1	2	3
Unwelded	2.3 Mg./cm.^{2}	1.1	1.9	6.9
Welded	1.7 Mg./cm.^{2}	0.6	1.1	5.0

Table 30—Corrosion of Welded Aluminum in Baltic Sea Water. Anon.[216]

		Coating		
	Uncoated	1	2	3
Unwelded	6.0 Mg./cm.^{2}	2.5	1.5	7.3
Welded	4.6 Mg./cm.^{2}	...	1.8	15.0

Specimens (12 x 12 inches x 0.12 inch thick) were suspended by Al wire from a wooden bridge so as to dip 5 feet and 15 feet below the surface in Baltic sea water, Table 30, which was in slow motion, mainly clear, and 0 to 20° C. A rubber washer prevented metallic contact between suspension wire and specimen. Some of the specimens had dropped from their supports and all were heavily coated with barnacles. The weld exerted no electrolytic action on plate and had no appreciable effect on corrosion as compared with unwelded. The corrosion at 15 feet submersion was a little less than at 5 feet because the oxygen content of the water decreased with depth. The composition of the waters is shown in Table 31.

Table 31—Composition of North Sea and Baltic Sea Water. Anon.[216]

	North Sea	Baltic Sea
KCl }	2.558	0.052
NaCl }		1.538
$MgCl_2$	0.257	0.195
$MgSO_4$	0.100	0.114
$MgCO_3$	0.020	0.0105
$CaSO_4$	0.220	0.091
Total	3.155	2.000

1929—Dunlap[393]

One set of specimens of commercially pure aluminum welded with commercially pure aluminum and 5% silicon wires was tested in 20% salt spray for 6 months and another set in hard tap water for 45 weeks. Visual examination showed no difference between the two types of welds.

1928—Anonymous[217]

Welds made in commercial Al and pure Al (99.77% Al) by oxyhydrogen, oxycoal gas, and oxyacetylene processes were immersed 15 hrs. in 10% HCl. The welds in pure Al lost hardly any thickness in weld or plate, but the commercial Al welds were greatly reduced in thickness. Welds made by all processes gave the same result, which is regarded as refuting the argument that the corrosion resistance of oxyacetylene welds is damaged by inclusions of aluminum carbide in the weld.

1925—Lebrun[218]

Gas welds in pure Al plate corroded faster in tests (no details) than plate metal. A small addition of copper to the rod improved the corrosion resistance.

1922—Le Grix[219]

Rapid corrosion of welds in Al plate in tests (no details) is due to innumerable, microscopically invisible blow holes which are flattened out during rolling but regain their original form from the heat of welding. Corrosion resistance is improved by adding 1% Cu to the filler rod. If the plate contains 1% Cu, the filler rod should contain 2% Cu for best corrosion resistance.

General Observations on Aluminum Welds

Aside from flux, which is discussed in a separate section, gas and slag inclusions have been mentioned[220] as most detrimental to the corrosion resistance of aluminum welds, particularly in HNO_3.[155] Guertler[220] also notes the damaging effects of overheating on corrosion resistance, but welded brewery equipment, in his experience, practically never develops undue corrosion at welds. Grain size, according to Buchholz,[220] has only a slight effect on corrosion resistance of welds. Cold hammering, however, is damaging, as another writer[221] also states, and simultaneous two-sided welding develops best corrosion properties. In Groebler's[396] opinion, corrosion resistance is lowered if hammering is done below 300° C. Nevertheless, Eckert[222] recommends that welded Al nitric equipment be annealed and subsequently lightly cold hammered, and Eyles[223] states that hammered and burnished Al welds have the same corrosion resistance as base metal. Buchholz[220] believes that gas and hammer welds are best in corrosion, but Röhrig[224] found that hammer welded Al tanks for HNO_3 were rapidly attacked along the joint. Gas and metal arc welds were quite satisfactory, especially hot hammered. Maier[225] also states that hot hammering raises the corrosion resistance.

According to Zimmermann,[220] shrinkage stresses may act like cold work to accelerate the corrosion of Al welds. An instance of grain boundary corrosion of gas welds in Al by a reducing organic base at elevated temperatures is mentioned by Werner,[226] who believed that the oxide in the grain boundaries was attacked by the reducing agent. Hammer welds were not affected by the reagent. Groebler[396] observed intercrystalline corrosion only in coarsely crystalline welds. The importance of using pure Al filler rod is emphasized by Lüder[227] and Froelich.[228] Birett[229] gives an example of concentrated corrosive attack on Al welds not properly treated in the Eloxal process. The anodizing of Al should be done after, not before spot welding, and the corrosion resistance of the anodized welds apparently may not be entirely satisfactory, Bohn[230] states. Spot welds in aluminum have satisfactory corrosion resistance, according to Schraivogel.[231] Three types of tests for the corrosion resistance of Al welds are suggested by Magnani and Panseri:[232] atmospheric exposure, salt spray and immersion in specific reagents.

Experimental Corrosion Tests of Welded Alloys of Aluminum, Including Alclad

1937—Hoglund[201]

Tensile tests on spot welds in 52 S½H, 53 ST, Alclad 17 St, and Alclad 24 STR after exposure to the atmosphere for one year at Point Judith, R. I., where salt spray is deposited on the specimens in stormy weather, revealed no decrease in strength. Slight losses were obtained on testing spot welds in 17 ST and 24 ST (no details). After two years exposure there was no evidence that the strength had changed to any extent.

Visual examination of spot-welded panels in 52 S, 53 S, Alclad 17 ST, Alclad 17 SRT, Alclad 24 ST, and Alclad 24 SRT subjected to 6300 tidal immersions (10½ summer months) in brackish Hudson River tide water (containing appreciable quantities of industrial waste) at Edgewater showed no preferential attack of the welds. The panels were so exposed that at hourly intervals the lower portion is immersed continually in water, the middle portion is immersed alternately, and the top is exposed to air. Spot-welded panels in 51 S, 17 S and 24 S showed some preferential attack of the welds in the completely immersed and alternately immersed portions of the panels.

1937—Miss Goldowski[400]

Coloroscopic test with pH indicator, when applied to Duralumin sheet welded with Duralumin rod (no details) of the same composition, revealed a thin red anodic zone on both sides of the weld and corresponding to the heat-affected zone. Similar zones were found in thick Duralumin plates on which a soldering torch had been played. An unheat-treated welded Duralumin tank developed broad colored zones which disappeared after heat treatment. The specimen for coloroscopic test of light metals must be dipped for 30 seconds in hot technical NaOH, then neutralized in 50% HCl, and finally cleaned in water. The indicator (changes from red to violet with change from pH = 2 to pH = 12; proprietary indicators) is applied in 1% salt solution thickened with gelatin to stabilize the color pattern.

1936—Hoglund[204]

Spot welds in Alcoa 52 S (2.5 Mg, 0.25 Cr), 53 S (1.25 Mg, 0.7 Si, 0.25 Cr), and all Alclad (aluminum-coated Duralumin) alloys. As with Al, the spot-welded alloys suffered no significant losses after exposure to salt spray for one year. Alternate immersion tests of spot-welded Alclad boxes in the Hudson River tidewater at Edgewater, New Jersey, showed that spot welds had the same corrosion resistance as riveted joints.

1936—Schraivogel[231]

The corrosion resistance of Duralumin is lowered by spot welding owing to segregation which occurs in the alloy on heating. Intercrystalline corrosion was found beyond the weld spot at a point in which no change in microstructure could be detected.

No corrosion occurred in spot-welded Alclad subjected to 3% NaCl solution for 6 months. The weld spot was surrounded by the aluminum surface layer.

(Original report not available to the reviewers.)

1936—Bürgel.[205]

See section on Experimental Corrosion Tests on Welded Al. Gas welded Hydronalium (7 to 8 Mg, 0.4 Mn, 0.3 Si) tested in the same way as pure Al welds. The Hydronalium was heat-treated after welding; the welds behaved about the same as base metal. Quantitative results are not given.

1936—Manevetz[233]

Spot-welded Duralumin immersed for 272 hours in aerated sea water containing 0.5% H_2O_2 showed white deposits on the sheared edges and on the circumference of the spots, the spot welds themselves being stained black, although there was no apparent microstructural change.

1936—Auchter[399]

In salt spray tests welds in Silumin (14 Si, 86 Al) were anodic to base metal, but welds in KS-Seewasser (2.2 Mg, 1.3 Mn, 0.7 Si, 0.2 Sb) were cathodic (original report not available to reviewers).

1935—Röhrig and Nicolini[208]

Gas-welded Albondur (Alclad), Pantal (0.5–1.0 Mg, 0.4–1.4 Mn, 0.8–2 Si, 0.3 Ti), Hydronalium soft (5–10 Mg), KS-Seewasser soft and half-hard (2.2 Mg, 1.3 Mn, 0.7 Si, 0.2 Sb), BS-Seewasser (7.5 Mg, 0.3 Mn), 0.04 inch thick. There was no appreciable loss in strength after tidewater corrosion in North Sea water for 18 months (original report not available.)

1935—Prot[234]

Coloroscopic test with pH indicator, applied to welds in Duralumin sheet and tube, revealed inhomogeneities between weld and plate metal, and agreed with service results.

1934—Bohn[230]

Spot-welded Alclad subjected to extensive salt spray tests (no details) showed no observable loss in strength of the spots.

1934—Anastasiadis[235]

Metal-arc welds (electrode negative) in Hydronalium (5 to 10 Mg, 0.5 Mn, some Si, 0.16, 0.20, 0.24 and 0.32 inch thick) were totally immersed in a slowly agitated aqueous solution containing 3% NaCl, 1% H_2O_2. Every 24 hrs. the H_2O_2 content was controlled by analysis. The results are shown in Table 32. Local corrosion pits due to oxygen concentration cells were observed in plate metal but not in weld or transition zone. Anastasiadis concluded that the mechanical properties of weld metal and joints were not significantly changed by corrosion for 12 days.

Table 32—Results of Saline Corrosion Tests. Anastasiadis[235]

Time of Test, Days	Unmachined Weld Tensile Strength, psi	Unmachined Weld Elongation, %	Machined Weld Tensile Strength, psi	Machined Weld Elongation, %	All-Weld-Metal Tensile Strength, psi	All-Weld-Metal Elongation, %
0	30,000	..	26,400	3½	28,500	5
3						4
6						3
9						4
12	28,500	..	25,200	2½	27,000	4½

1930—Bauermeister[236]

Welding cast KS-Seewasser alloy (3 Mg, 3 Mn, 0.93 Sb, 0.56 Fe, 0.26 Si, traces Zn, Pb) had no appreciable effect on resistance to corrosion by sea water.

1929—Dunlap[237]

Specimens consisting of two 3/16-inch spot welds in a one-inch strip (overlap joints) were subjected to alternate immersion tests in a solution containing NaCl and H_2O_2 (no details) for 48 hours. The change in strength of the spot welds was negligible, except in 51 SW, as shown by Table 33.

Table 33—Alternate Immersion Saline Corrosion Tests of Spot Welds. Dunlap[237]

Specimen	Strength, Lb. Uncorroded	Strength, Lb. Corroded	% Change
Alcoa 17 ST	807	800	− 0.87
Alclad 17 ST	1047	1038	− 0.86
Alcoa 51 SW	740	666	−10.00

(17 ST contains 4 Cu, 0.5 Fe, 0.6 Si, 0.6 Mn, 0.6 Mg, heat-treated; 51 SW contains 1.9 Cu, 0.6 Mg, 1.0 Si, wrought.)

After 26 weeks in a 20% NaCl salt spray the results in Table 34 were obtained (average of 10 specimens).

Table 34—Salt Spray Tests of Spot Welds. Dunlap[237]

Specimen	Strength, Lb. Uncorroded	Strength, Lb. Corroded	% Change
17 ST	775	733	−5.16
Alclad 17 ST	1007	1047	+3.97
51 SW	689	692	+0.43

Microexamination of the 51 SW specimen subjected to salt spray revealed no evidences of corrosion. The Alclad coating over the spot weld in Alclad 17 ST was destroyed in the salt spray test but the surrounding metal was unaffected. Therefore the Alclad coating acts electrolytically to protect the remainder of the metal. The 51 SW and Alclad specimens showed no attack at some distance from the welds but the 17 ST was pitted.

1929—Anonymous[216]

Conditions identical with those described (same reference) in the section on pure Al. Gas welded Duralumin (4 Cu, 0.5 Mg, 0.25 Mg, 0.25 Mn) in North Sea water (Table 35).

Table 35—Gas-Welded Duralumin in North Sea Water. Anon.[216]

	Loss in Mg./Cm.² Coating: Uncoated	Coating: Electrolytic	Lead Spray
Unwelded	2.4	2.0	7.5
Welded	2.5	2.5	0.7 (weld heat-treated)
Riveted	2.6	...	8.3 (lap joint)
Riveted	1.2	...	4.5 (butt joint)

Corrosion was heaviest at the edges of the specimens. The results of immersion for 8 months in Baltic Sea water are shown in Table 36.

Table 36—Gas-Welded Duralumin in Baltic Sea Water. Anon.[216]

	Coating: Uncoated	Coating: Electrolytic	Lead Spray
Unwelded	6.7	3.7	40.0
Welded	15.0	14.6	9.1
Riveted	10.6		8.7

Coating	Heat-Treated	Depth of Water, Ft.	Total Loss of Weight, Mg.	Local Action: Weld	Local Action: Plate
None	Yes	15	8.4	Heavy	Moderate
Lead Spray	No	5	9.1	Heavy	Heavy
None	No	15	9.8	None	Heavy in heat-affected zone
Electrolytic	No	15	10.1	None	Heavy in heat-affected zone
None	No	5	13.5	Heavy	Heavy in heat-affected zone
Electrolytic	No	5	14.6	None	Heavy in heat-affected zone
None	No	5	15.0	None	Heavy in heat-affected zone
Lead Spray	Yes	15	18.2	Heavy	Moderate

In specimens not heat-treated the corrosion is concentrated in two strips on both sides of the weld, uncoated or electrolytically coated. The effect was much more pronounced in the wide specimens in the Baltic water than in the narrow specimens in North Sea water. In heat-treated specimens, weld was more heavily attacked than plate, zone corrosion being absent. The zone corrosion occurred even with protective coatings.

Potential measurements on the corroded Duralumin specimens gave the following results: 0.11 volt difference between heat-treated and unheat-treated welds. In unheat-treated welds there was a difference of 0.06 volt between weld and uncorroded edge of plate in the sense that weld should be corroded. Actually the corrosion tests showed that the reverse occurred. Between uncorroded plate and plate in corroded zone there was a potential of 0.05 volt, corroded zone anodic.

1929—Downes[392] (U. S. Naval Aircraft Factory)

Duralumin strips (3 x 1 inch) were spot welded together in pairs to produce specimens 5 x 1 inch with a spot at the center of the 1-inch overlap. A machine regularly used for steel was employed. A strip of steel 0.30 inch thick was inserted between each electrode and the Duralumin. No flux was used. The spots were $^3/_{16}$ to $^5/_{16}$ inch diameter and almost all had cracks and internal porosity. The spot diameter increased with thicker sheets. The heat treatment was 20 minutes at 500° C. in molten mixture of sodium and potassium nitrates, followed by quenching in water at 60° F. (15° C.). The specimens were then aged 10 days at room temperature. The corroding medium was 20% salt water spray.

The results of tension tests on the corroded samples are shown in Table 37. The averages are for five specimens each. After 30 days in salt spray little, if any, accelerated corrosion at or near the welds could be detected. After 60 days increased corrosion was manifested in some specimens by a ring of pits concentric with the weld and about $^3/_{32}$ inch therefrom, probably caused by electrolytic action. It is not stated whether the Duralumin sheet was heat-treated or cold rolled before welding.

Table 37—Spot-Welded Duralumin in 20% Salt Spray. Downes[392]

Sheet Thickness, Inch	Heat Treatment After Welding	Breaking Load, Lb. Max.	Min.	Avg.
0.014	None, uncorroded	216	136	173
0.014	Heat treated and aged, uncorroded	320	155	250
0.014	Ditto, 60 days salt spray	260	80	170
0.029	None, uncorroded	580	336	466
0.029	Heat treated and aged, uncorroded	705	545	623
0.029	Ditto, 60 days salt spray	670	412	580
0.045	None, uncorroded	810	512	656
0.045	Heat treated and aged, uncorroded	1130	805	1009
0.045	Ditto, 60 days salt spray	1025	745	931
0.063	None, uncorroded	1079	668	882
0.063	Heat treated and aged, uncorroded	1055	415	756
0.063	Ditto, 60 days salt spray	1392	240	1049

General Observations on Aluminum Alloy Welds

As pointed out by Jeffries, Nagel and Wood,[238] the poor corrosion resistance of the heat-affected zone in Duralumin welds is due to loss of heat treatment. The type of precipitated Cu-Al particle in the zone is favorable to rapid corrosion. Joyce[239] found that heat treatment of the welds, quenching and aging, is necessary to prevent junction zone corrosion in Duralumin. Brenner[240] states that the corrosion resistance of gas welds in Hydronalium is slightly less than base metal. No more difficulty with corrosion need be expected with spot-welded Alclad than with riveted, according to Taylor.[241]

Intercrystalline corrosion of spot-welded Duralumin was observed by Schraivogel[231] in an area beyond the spot where no change in the microstructure appeared to have occurred. Spot-welded Alclad did not develop intercrystalline corrosion (6 months in 3% salt solution) because the spot was surrounded by the aluminum surface layer.

According to Portier,[408] short corrosion tests (no details) showed that structural changes in heat-treated aluminum-magnesium alloys caused by spot welding do not affect the corrosion resistance appreciably. This is not true of other heat-treatable aluminum alloys, particularly those containing copper. D. C. arc welds in aluminum alloys, according to Rogoshkin and Kostin,[242] have good corrosion resistance.

Hard Solders

The corrosion resistance of hard solders for aluminum has been investigated by Rostosky.[243] Specimens of Al joints made with different hard solders were immersed for 6 months in sea water, 1 to 19° C. The hard solder containing silicon (presumably 12% Si) gave the best results. Rostosky also described a hard solder (melting point 100° C. below Al, emf. + 0.0245 volt with respect to Al) which showed no marked corrosion after 139 hrs. in tap water or 96 hrs. in boiling 1% NaCl solution. The Technical Institute of Stockholm[244] state that a hard solder containing 9% Cu, 1% Ag, rem. Al is good under corrosive conditions. The 12% Si hard solder, according to Downes,[117] is quite resistant to electrolytic action and is electro-negative to Al. (See section on Corrosion of Soldered Joints.)

Flux

The effect of flux on the corrosion of welds in Al and its alloys is of prime importance, as shown by several investigators.[33,245] The action of the flux depends on its characteristics. According to Maier,[246] non-hygroscopic fluxes for Al actually make the weld more corrosion resistant than plate. It would appear, therefore, although no investigation of the matter appears to have been made, that the hygroscopic flux subjects the Al in its vicinity to the action of a concentrated solution, depending on humidity.

A number of methods for removal of flux have been proposed. The method recommended by the Aluminum Advisory Bureau (Egglesmann[247]) consists of brushing the joint with soda lye, followed by washing in water and dipping in 15% HNO_3. The joint is then washed in water until, when dilute silver nitrate solution is flowed over the joint, no opalescence appears. Froelich[228] states that flux should be removed before the joint is hammered. The joint is first scrubbed with a steel brush and hot water. Hot 5% H_2SO_4 or HNO_3 is then brushed on and allowed to react for a few seconds. Finally the joint is rinsed in hot running water, until the silver nitrate test (5% solution) is satisfactory. Downes[117] recommends 10% H_2SO_4 for removing flux, and Bowman[248] also gives directions for cleaning Al welds.

Corrosion of Welds in Copper and Its Alloys

Copper

Practically the only investigation of the corrosion resistance of welds in copper has been made by Hun-

Table 38—Continuous Immersion Tests of Gas-Welded Copper. Hunsicker[249]

Test Conditions	Corrosion Rate, Gram/M.²/Hr. Temperature of Annealing for 1 Hr. After Welding, ° C. 600	700	800	900	1000	Stretched 20% in Tension Before Welding
100% Acetic acid, 20° C., 64 hrs.	0.02	0.02	0.08	0.08	0.10	0.02
100% Acetic acid, 60° C., 2 hrs.	0.5	0.5	0.8	1.0	1.1	0.5
100% Acetic acid, 80° C., 3 hrs.	1.1	1.0	1.2	1.7	1.4	0.9
100% Acetic acid, 116° C., 2 hrs. (boiling)	2.2	2.5	2.2	2.3	3.0	2.0
	All Annealed Specimens					
75% Acetic acid, 20° C.	0.2 to 0.4					0.30
50% Acetic acid, 20° C.	0.7 to 0.9					0.75
50% Acetic acid, 100° C. (boiling)	0.2 to 0.6					..
25% Acetic acid, 20° C.	0.6 to 0.8					0.65

sicker.[249] Fore-hand, single 90° V, oxyacetylene welds were made with flux (analysis after drying at 110° C.: 56.9% B_2O_3; 25.8% H_2O (evolved at 450° C.); 8.5% Na_2O; 2.5% CaO; 0.11% Cu; 0.15% SiO_2; 4.0% Cl; 2.22% P_2O_5; trace Fe_2O_3; trace Fe_2O_3; no K_2O; no SO_3; loss on drying at 110° C. was 45.1% H_2O) in specimens of electrolytic copper containing 99.8 Cu; 0.04 Cu_2O; 0.01 Ni; 0.02 Fe plus Al; 0.01 P; traces Mn and S; no Ag, 0.8 x 4¾ inch x 0.39 inch thick, and were subjected to liquid and gaseous acetic acid. Corrosion was estimated as loss of weight in gm. per sq. meter per hr., the specimens after test being immersed in 20 to 30% HCl, washed in distilled water, and dried before weighing. The specimens were milled from the plate and the edges filed smooth; the weld reinforcement was not machined. The object of the tests was to determine the effect of the changes in grain size and of recrystallization as a result of welding and subsequent annealing on the corrosion resistance.

The results of the continuous immersion tests are shown in Table 38. Glass (uninsulated) flasks heated by Bunsen burners were used for the tests. A reflux device was used for the tests in boiling acid. It was found that the rate of corrosion was quite rapid in the first hour of testing, but after 2 hours of testing the corrosion rate settled down to a constant value. In some cases it was impossible to obtain reproducible results.

Although the proportion of weld surface was only 10% of the total surface of the specimens, the welded specimens showed about the same loss of weight as unwelded specimens. Specimens welded and annealed at 600 to 700° C. had about the same corrosion resistance as unannealed specimens. Higher annealing temperatures increased the rate of attack. As the concentration of acid was increased from 0 to 100% the attack at 20° C. attained a maximum at a concentration of 50% and decreased to nearly nil at 100%. Macro-examination revealed that the attack on the welded specimens was localized, apparently as pits, at the heat-affected zone. In fact, the specimens annealed at 1000° C. appeared to develop intercrystalline corrosion. Unfortunately, tensile tests of the corroded specimens were not made. The effect of stretching the copper 20% in tension before welding had practically no effect on corrosion resistance. The results in general do not agree qualitatively with the results of Rabald on the corrosion of unwelded copper in acetic acid.

The results of corrosion tests in pure acetic acid vapor at 96 to 105° C. with and without additions of air are shown in Table 39. The experimental arrangement was such that it was impossible to avoid the presence of water vapor in the gases, but no account seems to have been taken of it. The time of annealing between the limits 1 to 4 hours had no effect on corrosion resistance. Although, as Table 39 indicates, the results do not appear to be particularly consistent, it seems clear that annealing at 600 to 700° C. is beneficial, and that additions of air to the acetic vapor increase the rate of corrosion. As in the continuous immersion tests, the larger grain size caused by the higher annealing temperatures is given as the explanation for the increased corrosion rate in specimens annealed at 800 to 1000° C.

The effect of the welding flux in increasing the rate of corrosion was shown in Table 40 by heating two specimens (0.4 x 0.6 x 2¾ inches): one without flux, the other coated with flux on all sides, to welding heat (no details) and subjecting them to boiling 100% acetic acid at 116° C. Obviously the welding flux increases the corrosion rate, but the explanation is not at all clearly stated.

Table 40—Effect of Flux on Corrosion of Heat Affected Copper. Hunsicker[249]

	Corrosion Rate
Heated to welding temperature without flux	0.68 gm./m.²/hr.
Heated to welding temperature with flux	7.0 gm./m.²/hr.
Untreated base metal	2.2 gm./m.²/hr.

Since the most important characteristic of copper filler rods for welded chemical apparatus is good corrosion resistance, a copper-silver and a copper-nickel rod of the compositions shown in Table 41 were investigated. The

Table 39—Corrosion of Gas-Welded Copper in Acetic Acid Vapor. Hunsicker[249]

Test Conditions	Corrosion Rate, Gm./M.²/Hr. Temperature of Annealing, ° C. As-Welded	600	700	800	900	1000	Stretched 20% in Tension (20° C.) Before Welding
Pure acetic acid vapor	9.5	7.5	7.5	8.5	10.5	10.5	9.5
Ditto plus 7.5 liters air/hr.	16	14	14	14.5	14	18	14
Ditto plus 15 liters air/hr.	10	8.5	9	10.5	12	9	10.5
Ditto plus 22.5 liters air/hr.	18	..	20	22	24	23	14 to 21
Ditto plus 30 liters air/hr.	17.5	..	17.5	21	22	16.5	16 to 23

Table 41—Copper Alloy Rods for Corrosion Tests. Hunsicker[249]

	% Ag	Pb	Ni	P	S
Copper-silver alloy rod	0.89	0.00	0.035	0.01	0.00
Copper-silver deposited metal	0.8	0.00	0.025	0.012	0.00
Copper-nickel alloy rod	0.00	0.00	0.61	...	0.00
Copper-nickel deposited metal	0.00	0.00	0.59	...	0.00

Table 41A—Corrosion of Copper Alloy Weld Metal. Hunsicker[249]

Test Conditions	Rate of Corrosion Grams/M.²/Hr. Boiling 100% Acetic Acid 116° C.	Boiling 50% Acetic Acid 100° C.
Electrolytic copper, unwelded	2.2	0.3
Electrolytic copper, welded with electrolytic copper rod with flux, backhand	9.5	3
All-weld-metal copper-nickel alloy rod with flux, backhand	8.7	2
All-weld-metal copper-silver alloy rod with flux, backhand	3.2	2
All-weld-metal copper-silver alloy rod with flux, forehand	2.5	1.6

Table 42—Corrosion of Copper Alloy Welding Rods in 100% Acetic Acid. Hunsicker[249]

Rod	Rate of Corrosion Gm./M.²/Hr. Copper-Nickel Alloy	Copper-Silver Alloy
Bare rods 4 inches long, 0.08 inch diam.	0.78	0.57
Ditto welded end to end without flux	0.55	0.40
Ditto welded end to end with flux	0.70	0.26
Welded joint in copper 0.4 in. thick without flux	0.43	Gained weight
Welded joint in copper 0.4 in. thick with flux	1.2	0.65

deposited all-weld-metal specimens were machined from welds in 120° V joints using electrolytic copper plate. The results of corrosion tests are shown in Table 41A. Other tests in boiling 100% acetic acid, shown in Table 42, indicate that the flux, as well as the alloy content, are important factors. It should be noted that the corrosion rates given in Table 42 are much smaller than those reported in the preceding. Hunsicker does not explain the difference.

Table 43 gives the results of potential measurements of cells in 100% acetic acid of which one electrode was

Table 43—Potential Measurements on All-Weld-Metal. Hunsicker[249]

Cell	Temperature—Degrees Centigrade 20	40	60	80	100
Copper—electrolytic copper all-weld-metal	0.25	0.5	0.9	1.3	2.0
Copper—copper-nickel ditto	0.50	1.1	2.0	3.3	...
Copper—copper-silver ditto	0.00	0.1	0.3	0.5	0.7

electrolytic copper, the other all-weld-metal. The specimens were 0.12 inch apart in the solution, and the potential is given in millivolts. The increase in potential with temperature is similar to the increase in corrosion rate, which leads Hunsicker to conclude that corrosion of copper welds in 100% acetic acid is primarily electrochemical. The important fact emerges that for all-weld-metal deposited by the copper-silver rod, the current flows from copper to weld metal. The reverse is true for all-weld-metal deposited by electrolytic copper or copper-nickel alloy rods. Potential measurements in 50% acetic acid were not consistent, unexplained reversals in potential being observed in the course of the tests. Hunsicker does not relate the potential measurements to preferential corrosion in the heat-affected zone, which, indeed, he does not mention in connection with welds made with alloy rods.

Hunsicker concluded that the silver alloy filler rod gives the most corrosion resistant welds; electrolytic copper rods, the least. Flux should be washed off to improve the corrosion resistance. The surface of the weld is more acid resistant than base metal owing to a surface film of undetermined composition. The heat-affected zone on the surface of welds made with copper rod is not preferentially corroded to any extent. If the cross section of the weld is exposed, however, the heat-affected zone is heavily attacked. Film formation is possibly responsible for this behavior.

No other systematic corrosion tests of copper welds appear to have been made. Leroy and Bonnot[88] state that blow holes are of prime importance in the corrosion of copper welds, whereas Millar[250] emphasizes the harmful effects of cuprous oxide inclusions. Neither factor is emphasized by Hunsicker, as noted above, who considers grain size and flux most important. Geiger[251] and Reavell[58] state that arc-welded copper is less corrosion resistant than oxyacetylene welded copper, and Gross[46] found that welded copper (1930) gave poor results in acidified $(NH_4)_2 SO_4$ solution.

Although available information based on tests under restricted conditions suggests that arc-welded copper is less corrosion resistant than oxyacetylene welded copper, there is probably little doubt, in view of recent developments in arc welding, that there is no significant difference between welds made by the two processes. According to J. R. Freeman (private communication, June 1937), there are many instances of satisfactory corrosion resistance of arc-welded copper in service.

Copper Alloys

Bürgel[205] subjected oxyacetylene welds in special bronze (50 Cu, 38 Zn, 10 Ni, 2 Mn) made with flux, pickled and neutralized in 1% HNO_3 after welding, to 23 reagents, and moist and dry air. The reagents comprised 10 dye chemicals, 7 acids, 3 fuel oils, acetone and lubricating oil. The test was for 20 hours at 65° C. Quantitative results are not given. The welded bronze specimens had better corrosion resistance than base metal in all reagents. Annealing was of no advantage.

Melhardt[252] states that hammering and subsequent heat treatment increases the corrosion resistance of oxyacetylene welds in brass but gas welded inserts in brass condenser tubes required no heat treatment to develop good corrosion resistance. Rawdon[253] attributed the cracking of burn-on welds in cast manganese bronze in service to a combination of atmospheric corrosion and internal stress. The internal stress amounted to 8500 to 9000 psi (proportional limit 13,000 psi). Block[254] points out that to prevent local action in welds in copper- or nickel-clad steel, the inner weld must be made with copper or nickel filler rods of the highest purity. According to Logan,[383] specimens of copper-silicon alloy pipe (12 x 1.7 x 0.145 inch, 98.11 Cu, 0.14 Sn, 0.01 Ni, 0.11 Fe, 0.18 Mn, 1.49 Si) with bronze welds at the middle (no details) exhibited no definite evidence of corrosion which could be attributed to galvanic action after two years exposure in peat soil.

Corrosion of Welds in Nickel and Its Alloys

Nickel

Several writers[256] have stated that the corrosion resistance of good nickel welds is the same as base metal. Boutté[257] observed no preferential attack of gas welds in nickel by HNO_3, and Horn and Geldbach[258] found that hammer welds in nickel have good corrosion resistance. Flocke[259] states that welding rod for nickel and its alloys should have the same composition as base metal to avoid electrochemical action. Welded nickel dairy equipment is perfectly reliable, according to Hunziker, Cordes and Nissen,[260] but soldered nickel has unsatisfactory resistance to dairy products. The results of service corrosion tests of welded nickel-clad steel have been excellent, as Flocke and Schoener[261] report.

Hignett[262] cites an example of stress-corrosion cracking of a metal arc weld in 1/4-inch nickel plate immersed in 5% acetic acid for two days. The failed specimen exhibited severe intercrystalline cracking in the heat-affected zone. Another arc weld stressed longitudinally at 27,000 psi failed in the same way as the unstressed. Since oxyacetylene welded specimens were unaffected by the test, it may be concluded that nickel under stress, such as that caused by arc welding, is sensitive to acetic acid corrosion.

Inconel

According to Flocke and Schoener,[263] Inconel welds of good quality show no attack after long periods in acid refrigerating brines and are at least as corrosion resistant as unwelded sheet. A factor contributing to the good corrosion resistance of welds is that Inconel is relatively free from disturbances due to strain and recrystallization. After 100 hours in acid $CuSO_4$ Inconel welds developed no intergranular corrosion. The freedom from intergranular corrosion is ascribed to the low iron content of Inconel, but other factors must also be at work.

Müller[264] also observed no loss of ductility in Inconel welds after 100 hours in boiling acid $CuSO_4$ solution.

Monel Metal

Schüppel and Kästner[265] made an extensive investigation of Monel welds in reagents commonly used in the dye industry. Oxyacetylene welds in Monel Metal made with a slightly reducing flame without flux were tested in a number of dye reagents. The specimens were in the form of tensile coupons of two sizes: 11.0 inch x 0.86 inch x 0.12 inch (reduced section 0.47 inch x 2.8 inch) and 10.2 inch x 0.47 inch x 0.055 inch (reduced section 1.6 inch x 0.32 inch). The welds were probably not machined. The appearance, loss of weight and tensile properties of the corroded specimens are listed in Table 44.

The tensile strength of unwelded, uncorroded Monel Metal was 76,000 psi, 27.3% elong. in 2.8 inch (30.5% in 1.6 inch) and 45.7 to 50% reduction of area. Uncorroded oxyacetylene welds had tensile strengths varying from 64.5 to 102.1% of unwelded, uncorroded Monel. The elongation of the uncorroded oxyacetylene welds varied from 6.3 to 30.9% in 2.8 inches.

Schüppel and Kästner found that the welds were no more heavily attacked than base metal. Unwelded samples were not included in the corrosion tests. Basing their judgment on loss of weight determinations (the tensile values being erratic), Schüppel and Kästner reached the following conclusions. The welds were not resistant to the $CuSO_4$—acetic acid pickle, and were of doubtful value in the hypochlorite liquor and concentrated formic acid. They were entirely resistant to the other reagents, as well as to a number of technical dye reagents.

Bürgel's[205] tests of oxyacetylene welds in Monel made in the usual way with flux showed that the welded specimens had the same corrosion resistance as unwelded. The welds were pickled after welding and neutralized in 1% HNO_3. They were corroded for 20 hours at 65° C. in 25 reagents, including moist and dry air, 7 acids, 10 dye chemicals, 3 fuel oils, lubricating oil and acetone. Marples[256] cites an oxyacetylene welded pickling crate made of 1/8-inch Monel sheet as indicative of the corrosion resistance of Monel welds. The crate, which was used for pickling steel in 10% H_2SO_4 or 24% HCl, gave 3 years of continuous service without developing signs of corrosion. Intergranular corrosion was absent. The International Nickel Co.[266] recommend a welding rod for Monel to be used for pickling that is galvanically neutral to base metal. An anonymous writer[267] believes that the best corrosion resistance is assured in Monel welds if the atomic hydrogen or oxyacetylene process is used.

Table 44—Corrosion Tests on Oxyacetylene Welded Monel Metal. Schuppel and Kastner[265]

Gage Length of Specimen, Inch	Reagent and Time of Test	Appearance After Test	Original Wt., Gm.	Loss of Wt., Gm.	Tensile Strength in % of Unwelded Uncorroded	Elongation, %	Reduction of Area, %
2.8	Hypochlorite liquor 1 to 3° Bé., about 7 wks.	Sooty black and green coating, mainly NiO (Ni-peroxide)	132.8	0.06	44.3	...	..
1.6	Hypochlorite liquor 3° Bé., 2 wks., 50° C.	....	35.9	0.321	100.5	27.7	44.0
1.6	Ditto	....	39.3	0.539	92.6	18.0	..
1.6	Hypochlorite liquor 25° Bé., 3 wks., cold	....	39.2	0.24	70.0	5.5	..
1.6	Ditto	....	37.6	0.379	87.1	10.0	..
2.8	Bleaching bath, 3 weeks	Mat green coat, no pits	134.5	0.09	40.0	...	...
2.8	Dilute H_2SO_4, 7 weeks	....	128.2	0.01	81.8	14.0	24.3
1.6	Ditto	....	39.7	0.0015	56.8	...	..
1.6 or 2.8	Hot and cold soap solutions, 4 to 7 wks. (no details)	No change	40 or 120 gm.	Up to 0.01	83 to 101.5	13.45 to 30.7	34.2 to 44.0
2.8	Hot water (3 to 4° hard) 3 weeks	No change		None	81.0	11.3	..
2.8	Formic acid, 60° C., 2 weeks	Thin grayish green coat, easily removed	133.4	0.029	84.0	15.7	...
1.6	Dilute formic acid, 6 weeks	....	39.5	0.001	102.5	29.9	41.0
2.8	Schwefelschwarz Färberei, 4 weeks	Black color; tiny pits	128.8	0.013	81.6	10.72	..
1.6 or 2.8	Sodium sulphide solution, cold, 8 weeks	Corrosion product contained FeS mainly	136.5 or 39.9	None	51.0 to 85.4	6.0 to 11.5	..
2.8	Mixture of acetic acid and $CuSO_4$, 8 weeks	....	133.9	0.8	66.7	5.43	..

Corrosion of Welds in Other Non-Ferrous Metals

Magnesium

Systematic corrosion tests of welded magnesium have not been reported. Bürgel[205] mentions that he tested some welded Electron (92–95 Mg, 5–7.5 Zn) for corrosion resistance, but he gives no data. Horn and Tewes[268] found that pickled gas welds in electron are superior in general corrosion resistance to base metal, on account of vaporization of zinc. The seam is first washed in water, then pickled in HCl-chromate bath which forms a bronze-colored corrosion resistant coating. In castings the fine grain of the weld metal also promotes corrosion resistance. After several hours in salt water, the base metal of welded electron plate was heavily attacked, but the weld was only slightly affected.

Auchter[399] (original report not available to reviewers) states that welds in Electron tend to pit in salt spray tests.

Jeffries, Nagel and Wood[238] state that in order to prevent corrosion of welds in Mg alloys, flux should be removed as well as copper particles picked up from spot welding electrode tips. An anonymous writer[269] in a German aircraft magazine mentions the excellent corrosion resistance of electron castings welded with special rod and flux (no details).

Zinc

Horn[270] states that the corrosion resistance of gas welds in 99.99% zinc is good (no details).

Lead

A welded Roman lead water pipe 2000 years old was recently unearthed and found to have withstood corrosion satisfactorily, according to King and Gray.[271] Service corrosion results on welded lead, soft or antimonial, are as satisfactory as on unwelded lead, Zigfield[272] states, and Okamoto, Nishimura and Onishi[375] found that the corrosion resistance of carbon-arc or metal-arc welded lead in H_2SO_4 was satisfactory. Welds in lead pipe appear to have no effect on corrosion by flue gas containing 1 to 8 grains sulphur per 100 cu. ft., and 6.5 to 9.2% CO_2 for 6 weeks at 58° F., as shown in tests by the Institution of Gas Engineers.[273]

Although McKellar[402] found that air-hydrogen welded lead boxes $^1/_{16}$ inch failed rapidly by intercrystalline corrosion in H_2SO_4 under certain conditions, welding did not appear to be at fault although internal stresses were a contributing factor to failure.

Corrosion of Welds in Galvanized Steel

Since the welding or brazing of galvanized steel by almost any method causes loss of zinc by evaporation from the immediate vicinity of the weld, experimental and service investigations, of which several have been made, are the only basis for evaluating the corrosion resistance of welded galvanized steel. The investigations show that, in many cases, regalvanizing or painting the welds is not necessary and that the process of welding employed has an effect on results, which, however, is not clearly explained.

Bibber's[274] investigation provides us with most of our information on the corrosion of welded galvanized steel. He prepared three kinds of specimens welded with bare or coated electrodes in $^3/_{16}$- and $^1/_2$-inch medium steel plate with 2 to 2 $^2/_3$ oz. Zn (98% pure) per sq. ft. (actually the thickness was 0.003 to 0.006 inch): (1) plate with single bead deposited on surface; (2) single- and multi-layer fillet weld; (3) single-layer lap welded joint with open lap. Chemical analysis revealed the absence of zinc in deposited weld metal. The corroding medium was 4% sea salt spray, which destroys one coat of paint in 500 hrs., two coats in 1100 hrs. After 1100 hrs. the single-layer welded specimens showed practically no attack although the galvanizing apparently was burned off. Welds made with bare and coated electrodes showed rust stains to about the same extent. The multi-layer fillet welded specimens, however, showed a considerable amount of rust after 1100 hrs. in the salt spray. In explaining his observations, Bibber states that the supposedly bare area in the immediate vicinity of the weld may perhaps be coated with a layer of zinc too thin to detect microscopically, or may be anodically protected by adjacent zinc. In the deposition of a single bead some zinc vapor sublimates on the hot deposited metal and forms a protective coating or compound. It should be noted that the anodic protection of bare spots by adjacent zinc depends on the corroding medium and that Bibber could produce no direct evidence for what must be an exceedingly thin, and consequently relatively ineffective, film of zinc on the deposited metal.

A series of interesting corrosion tests of fusion-welded and bronze-welded galvanized steel has been reported by Hooker,[275] who concluded that bronze-welded joints were superior to fusion welded. The basis for the conclusion was that bronze-welded specimens of galvanized sheet, prepared in several ways and also coated with aluminum paint after galvanizing, always showed less attack in salt spray tests (no details) than fusion-welded specimens. Hooker states that the electrochemical couple set up by bronze-welding is less than the iron-zinc couple created by fusion welding with steel filler rods. A bronze-welded joint in galvanized steel was not affected by immersion in $CaCl_2$ solution for 53 days at room temperature, although some iron and zinc was detected in the solution after test. Exposure of bronze-welded galvanized pipe for $2^1/_2$ years to the weather on the roof of an industrial building caused only a very small amount of rust where the galvanizing was affected by the heat of bronze-welding.

Three anonymous[276] writers also state that bronze-welding is better than fusion welding for galvanized steel, for the reason, as one points out, that less zinc is lost in the former process. Declan,[277] however, found that fusion welding did not decrease the corrosion resistance of galvanized pipe to tap water, but bronze-welding and threading were harmful. An anonymous writer[278] found that bronze-welded joints in galvanized pipe are more corrosion resistant than soldered.

Two other experimental investigations of the corrosion resistance of galvanized pipes may be mentioned in addition to those of Bibber and Hooker already discussed. Owens[6] subjected spot-welded galvanized iron sheet to alternate tidal immersion in sea water for two months but found no appreciable corrosion. Danieli[279] joined two plates of galvanized steel along one edge by flash welding, and exposed the specimen to the weather for two years. One plate had an electrolytic zinc coating 0.66 oz./ft.^{2}. The other had a hot-dipped zinc coating 1.1 oz./ft.^{2}. The hot-dipped sheet, after test, showed rust spots along the welded edge; the electrolytic galvanized sheet did not. Neither plate in the unwelded condition showed any rust after exposure to the weather for two years.

Welded and brazed specimens of galvanized steel, 0.20 in. thick, were subjected by a French organization[280] to a saline solution (no details) for several months. The welded specimens quickly rusted at the weld and adjacent areas. There was uniform attack of the zinc coating on the brazed specimens but no rust developed.

Service corrosion results of bronze-welded galvanized

iron acetylene generators, according to Greene,[281] have always been satisfactory and he knew of no instance in which premature failure of galvanized pipe could be attributed to preferential corrosion of the welded joint. Although zinc is removed at the welded joint, a protective oxide coating is formed which renders the brazed joint more corrosion resistant than threaded joints. Greene also believes that a thin film of bronze also can be relied upon to spread over and protect the heat-affected surface in the vicinity of the joint. Referring to gas welded galvanized pipe, Wikoff[282] states that the local loss of the galvanizing on the inside of the pipe near the weld has never been known to lead to difficulties. He points out that the galvanizing is also removed to some extent in threaded joints.

Much has been written concerning methods of welding galvanized material but the center of discussion is usually the brittleness (iron-zinc compound) rather than the corrosion resistance of the joint. For example, A. E.[283] states that prior to welding, galvanized coat should be removed along a strip $^1/_2$ to 1 inch on either side of the weld. The exposed area should be covered with bituminous or aluminum paint after welding. Reinhard[284] is of the opinion that the welded seams of galvanized tanks should be re-galvanized, and Glenn[285] remarks that all parts of the welded brine systems of some refrigerator cars are galvanized after welding.

Loss of ductility in welds due to pickling and galvanizing has been noted by Boutté.[286] Sage[287] recommends the use of Everdur for bronze-welding galvanized iron sheet, and the use of a water spray to avoid loss of zinc during spot welding galvanized sheet is covered by a patent.[288] A detailed description of a method of welding cracks in used galvanizing tanks so that the weld is as resistant to molten zinc as the zinc-coated base metal is given by Liebetanz,[289] Frankenbusch[290] and Arcos.[291] Bare electrodes, flux and as few layers as possible are chief factors in the special procedure recommended by Liebetanz. During the welding of lead-coated steel, according to Partington,[292] the tinning should not be permitted to come through the lead, otherwise the corrosion resistance is lost.

Corrosion of Bronze-Welded Joints

Bronze-Welded Cast Iron

A comprehensive investigation of the corrosion resistance of bronze-welded joints of the collar type in cast-iron pipe has been reported by Starke.[293] The joints were tested in the following boiling solutions: 10% NaCl (144 hrs.), 1% HNO_3 (144 hrs.), 1% HCl (144 hrs.), 1% H_2SO_4 (72 hrs.), 10% NH_4Cl (72 hrs.). An electric current was passed through the joint in the last two solutions. No electrolytic action or preferential corrosion of bronze or cast iron was observed in any of the solutions. Starke also found that water containing CO_2 had little effect on bronze-welded pipe. The bronze usually acquires patina.

Owens[6] subjected cast-iron specimens welded with Tobin bronze to alternate tidal immersion in sea water for two months but observed no appreciable corrosion.

Intermittent immersion laboratory tests of brazed joints in cast iron have been made by Erlenmeyer.[294] The tests consisted of cycles of immersion for 3 hrs. in distilled water followed by 3 hrs. in the air, and continued for 10 days. The specimens had an immersed surface of 1.9 sq. in., the sawn surfaces and injured cast skin of the cast iron being coated with waterproof varnish (Häfelit). At the conclusion of the test it was noted that rust was localized mainly at the brazed joint. A continuous immersion test of a brazed joint in distilled water with access of air showed a loss of weight of 0.1306 gm.; a similar test without access of air showed a loss of 0.1217 gm. Ferroxyl indicator tests showed blue patches at the braze.

As a result of a corrosion study of copper in contact with iron (no details) and of one-year burial of bronze-welded cast-iron pipe in highly corrosive soil (no details), Harris[295] concluded that corrosion due to contact with copper increases as the amount of copper is increased. The increased corrosion resulting from contact with copper is uniformly distributed over the entire surface of cast iron, the cast iron adjacent to the copper sometimes appearing to suffer less corrosion than cast iron several inches away. The amount of bronze necessary to make a bronze-welded service connection in cast-iron pipe definitely will not induce corrosion. Harris states that his conclusions are confirmed by a four-year inspection by the U. S. Bureau of Standards.

Erlenmeyer[294] also measured the electrode potential of the cell: cast iron (no analysis)–bronze (57.72 Cu, 0.45 Su, 0.37 Fe, 41.53 Zn) as shown in Table 45. The cast-iron electrode with cast skin was 2 inches long, 0.20 inch square. The bronze electrode was 2 inches long, 0.32 in diam. The compensation method was employed with a capillary electrometer as null instrument. The tests showed that the cast iron is negative and goes into solution. Measurements (Table 46) were also made on two cast-iron electrodes, one with, the other without, cast skin. The electrode without skin was negative and went into solution.

Table 45—Intermittent Immersion Tests of Bronze-Welded Cast Iron. Erlenmeyer[294]

Electrolyte	Emf., Volt	Contact Potential, Volt	Preparation of Electrolyte
$Ca(HCO_3)_2$	0.533	0.08	CO_2 led into $CaCO_3$ suspension at 20° C.
H_2O saturated with O_2	0.410	0.02	Spring water saturated with tank oxygen
$CaSO_4$	0.525	0.10	Saturated solution (20° C.) with precipitate at bottom
NaCl-$MgCl_2$	0.394	0.20	2.5% NaCl, 0.6% $MgCl_2$
1% NH_4Cl	0.412	0.22	....
Peat slime	0.410	0.04	Suspension of fresh peat in tap water

Table 46—Electrolytic Potential Difference Between Cast Iron with and Without Skin. Erlenmeyer[294]

Electrolyte	Emf., Volt
$CaSO_4$	0.023
1% NH_4Cl	0.0724

Erlenmeyer also dipped a bronze and cast-iron electrode in $CaSO_4$ and short circuited them through a milliammeter. The current was less than 1 ma. with a contact voltage of 0.015. After 72 hrs. the Fe_2O_3 in solution corresponded to 0.1712 gm. Fe from about 2.2 sq. in. of cast-iron surface. A corresponding test with a Monel Metal and a steel electrode gave a loss of 0.1383 gm. Fe from the same exposed area. Erlenmeyer concluded that the potential difference between bronze-welding rod and cast iron was considerable, and that his tests were not favorable to bronze-welded joints in corrosive conditions.

It is only fair to state, however, that the experimental conditions adopted by Erlenmeyer were highly artificial,

and that his results are not confirmed by the tests of Ownes and Starke, discussed above, nor by observations on bronze-welded cast-iron pipe in service. Experience has shown, as Rockefeller,[296] Greene[281,297] and the Linde Air Products Co.[298] point out, that bronze-welded cast-iron pipe is highly satisfactory in corrosive service. Rockefeller provides the clue to the successful behavior of bronze-welded cast-iron pipe in service by noting that the bronze-welded joint has a lower electric resistance than any other form of joint. Logan[387] brazed together several cast-iron strips, 2 x 8 inches, and buried them in corrosive soil (no details). The iron appears to have been cast in a mold coated with portland cement to prevent sticking. Neither cement nor brazing flux was removed. After 8 years corrosion did not appear greater at the joint than elsewhere. Bridge[299] recommends cast-iron pipe with brazed joints or copper pipe with soldered joints for gas distribution in the most corrosive soils (life of bare steel pipe less than 10 years).

The situation appears to be admirably summed up by Tyler and Percival[300] who state that, ordinarily, galvanic action is not apparent in bronze-welded cast iron, and by Holler,[301] who emphasizes the importance of avoiding local elements. The latter states that cast-iron apparatus for handling acids should be welded with cast-iron rod. Rollason[302] states that non-ferrous joints never improve the corrosion resistance of cast iron. The British Engine, Boiler & Electrical Insurance Co.[303] observed that the cast iron was preferentially attacked in etched macrosections of welds made with copper-nickel or mild steel electrodes, and concluded without further evidence that such welds would not be satisfactory in corrosive conditions. Jürgens[304] is of the opinion that bronze welding should not be applied to cast-iron hydrants on account of the corrosion problem.

Bronze Welds in Other Metals

The corrosion resistance of bronze-welded galvanized iron pipe in Zürich (Switzerland) tap water (total immersion 6 months) and humid garden earth (5 months) has been investigated by Keel.[305] Fontox (60 Cu–40 Zn) and Sildo bronze-welding rods were used. After 6 months in tap water the inside of the pipe showed no corrosion. The outside showed a thin, tenacious layer of carbonate where the galvanizing had been destroyed by the temperature of bronze welding. On either side of the bronze a little rust appeared on the pipe. In distilled water numerous local rust spots appeared on both sides of the bronze weld. No rust was observed on the specimens that had been packed in humid garden earth. Tests by Stewart[306] showed that pipe joints made with silver solder were more corrosion resistant than joints made with ordinary brazing solders.

A bronze-welding rod or rather "silver" solder for stainless steel is described by Müller.[307] The chief constituent of the rod is manganese and the rod melts at 750° C. The description of the rod resembles that given by an anonymous writer,[308] whose "bronze" contained 30 to 70 Mn, 10 to 60 Cu, 10 to 50 Ni and melted at 850 to 1150° C. depending on composition. It is noted that Co, Fe, Ag, Cr, Al or Zn may also be added. Joints made with Müller's rod withstood a salt spray test for 10 days without developing intergranular corrosion. Flocke[259] briefly describes the U. C. C. white brazing rod that matches Monel in color and has good corrosion resistance.

Bronze-welded joints in copper made with a Cuprotectic rod (Cu–Cu_3 P eutectic alloy), according to an anonymous writer,[309] showed no corrosion in dry or liquid SO_2. There was definite attack by moist SO_2, but the joint itself was not attacked to as great an extent as the copper. Fetherston[310] states that galvanic effect is entirely absent in bronze-welded copper pipe.

The electrolytic disintegration of bronze welds in steam piping was investigated by Arnold[311] in 1898. Small quantities of fatty acids, originating in lubricating oil in the steam engine cylinders, formed organic salts of zinc which were soluble in hot water. Louis[312] (1911) attributed the failure of a bronze-welded joint in steam piping to an intergranular lead-tin constituent which is selectively attacked by the corrosive agents in the pipe. A more plausible explanation of the intercrystalline corrosion was offered by Huntington,[313] who ascribed the difficulty to the bluish delta constituent in bronze. Many years later, however, Williamson[314] repeated Louis's explanation to dispose of a case of intercrystalline corrosion in bronze-welding metal, which he had encountered. A convincing demonstration of the inherent corrosion resistance of bronze-welded joints is provided by a Roman iron ring soldered with Cu, which, according to Friend and Thorneycroft,[315] was made before 380 A.D. Upon being unearthed after over 1500 years in English soil, the ring showed no preferential attack at the copper weld, nor, for that matter, at a forge weld in another part of the specimen.

Corrosion of Soldered Joints

Although, strictly speaking, not a topic in the corrosion of welds, the corrosion of soldered joints, like that of bronze-welded joints, occurs under conditions in which local or general corrosion effects due to differences in chemical composition of joint and base metal are accentuated. The majority of investigations on soldered joints have been devoted to aluminum solders, which are therefore dealt with first in the following summary. Consistent with its title the summary does not discuss investigations, with one or two exceptions, which involved the corrosion resistance of solders as distinct from soldered joints.

Experimental Corrosion Tests of Soldered Joints in Aluminum

1935—Holler and Maier[210]

Joints composed of aluminum soldered to iron, copper or lead (0.16-inch plate) by means of tin solder were exposed to the atmosphere or fully immersed in tap water (170 mg. chloride per liter) or a solution of 3% NaCl in tap water for 2 months. Atmosphere and tap water had little effect, but the salt solution destroyed all the soldered joints in 9 days, as shown by complete loss of tensile strength.

1935—Zeerleder[316]

Soft soldered joints in aluminum fall apart after a few months in sodium chloride solution.

1935—Fuss[317]

Aluminum soldered with zinc has poor corrosion resistance. The corrosion resistance of aluminum soldered with cadmium is improved by heat treating the joint.

1934—Röhrig[318]

Soldered joints (solder contained zinc) in aluminum castings were treated by the Eloxal process (anodic oxidation). If A.C. only was used to produce the coat, the protection was satisfactory. The use of D.C. and A.C. combined gave an inefficient coat, the aluminum at the edge of the soldered joints being heavily attacked.

1934—Anonymous[319]

Soldered joints in aluminum and Lautal (4 Cu, 2 Si) were immersed for 48 hours in boiling 5% NaCl solution. The results are shown in Table 47.

Table 47—Corrosion of Soldered Joints in Aluminum. Anonymous[319]

Composition of Solder								
Al	Zn	Sn	Si	Cu	Cd	Fe	Soldered Joint in Aluminum	Soldered Joint in Lautal
0.20	64.64	...	..	..	33.96	0.82	Fell apart in 48 hrs.	No failure
Rem.	8.75	...	0.5	0.62	...	0.38	No failure	No failure
Rem.	...	...	2.16	4.10	...	0.38	No failure	No failure
14.74	76.36	0.36	0.27	7.60	...	0.55	No failure	No failure
0.70	80.52	18.35	0.28	..	...	..	Failed in $16^1/_2$ hrs.	Failed in 2 hrs.

1933—Scarpa[320]

An aluminum cable 0.8 in. long consisting of 40 Al wires in an Al sheath soldered by means of an alloy containing 45 Zn, 35 Al, 15 Sn was immersed for 4 months in 2% NaCl solution. A white superficial deposit had formed but the loss of weight (0.140 gm.) was trifling.

1932—Thews[321]

Electrolytic potential of hard solder for Al differs from Al by only 0.02 to 0.07 volt. The difference in potential between soft solder and Al is 0.25 to 0.40 volt. The corrosion resistance of soft solders decreases as the Al content is increased up to about 15% Al. But hard solders (Al base) exceed all soft solders for Al in corrosion resistance.

1930—Krushchov and Sharov[322]

Corrosion tests of 10 aluminum solders (original report not available to the reviewers).

1930—Röhrig[323]

Three soldered joints in aluminum plate (99.5% pure, 0.04 inch thick, hard rolled) and Lautal (0.04 inch thick, normal hardness) were made with the solders shown in Table 48. Overlap joints 0.32 inch long were tested. A soldering lamp and a bare copper were used. Solder No. 4 is a reaction solder (zinc); Solder No. 8 had a slight thermal arrest near 300° C. Solders 2 and 3 are, of course, hard solders. A large part of Solder No. 4 was lost by boiling. It was not necessary to clean the joints with Solders 1, 2, 3 and 4.

The specimens were hung in boiling 3% NaCl. In Table 48 failure means that the two strips joined by the solder fell apart. Solders 1, 4, 6, 7, 8, 9 and 10, it was concluded, are not corrosion resistant. Solders 2 and 3 showed no discoloration.

The results of tensile tests on soldered joints is shown in Table 49. The corrosion period was 48 hrs. in boiling 3% NaCl. The tensile strength is based on the load at fracture and the cross section of one plate. Although Solder No. 2 retained good strength after corrosion, it was badly oxidized.

Table 49—Tensile Tests on Soldered Joints. Röhrig[323]

Solder No.	Tensile Strength, psi: Aluminum, Before Corrosion	Aluminum, After Corrosion	Lautal, Before Corrosion	Lautal, After Corrosion
1	12,000 B	13,000	32,000	33,000
2	13,400 B	13,000	Not tested	Not tested
3	13,000 B	13,000	Not tested	Not tested
4	5,500 E	A	29,500 C	23,300 C
5	13,000 B	12,500 C	26,400 B	12,800 D
6	13,000 B	A	23,000 C	A
7	12,800 B	A	28,800 C	5,500 D
8	10,400 D	A	30,000 C	10,200 D
9	12,000 C	A	17,800 C	A
10	7,300 D	A	5,800 C	A

A—Failed in corrosion test
B—Fractured in plate
C—Failed partly in plate, partly in joint
D—Failed in joint
E—Insufficient solder, failed in joint

1928—Lüder[324] (summary of a doctorate thesis by H. Steinweg)

Al plate, soldered with Cd, Sn or Zn, was subjected to distilled water, tap water and 1% NaCl all at 100° C., and to acetic acid at room temperature. There was little corrosion except in acetic acid. The corrosion resistance of the specimens soldered with cadmium had good corrosion resistance if the penetration of cadmium was thorough. But if the Cd penetrated in an intercrystalline manner the corrosion resistance was bad. The effect of corrosion for 25 hrs. in distilled water was to reduce the tensile strength of the joints soldered with tin at or above 500° C. by 86%. Specimens soldered at lower temperatures corroded very rapidly. The best corrosion resistance of joints soldered with zinc was developed by soldering at 500 to 550° C. Except in acetic acid, the zinc soldered joints failed in 3 or 4 days. After 50 days in acetic acid, however, the zinc soldered joints had lost no strength (12,800 psi tensile strength). Additions of 1 to 5% Zn to Cd had little effect, but with 20% Zn the corrosion resistance was no better than with pure zinc.

Table 48—Corrosion Test of Soldered Al and Lautal. Rohrig[323]

Solder No.	Composition of Solder, Wt. %	Solidification Interval, ° C.	Results: Aluminum	Results: Lautal
1	64.64 Zn, 33.96 Cd, 1.02 Fe + Al	370–360	1 failed in 48 hrs.	None failed in 48 hrs.
2	0.50 Si, 0.62 Cu, 0.38 Fe, 8.75 Zn, rem. Al	630–616	None failed in 48 hrs.	....
3	2.16 Si, 4.10 Cu, 0.41 Fe, rem. Al	620–600	None failed in 48 hrs.	....
4	88.4 NH_4Cl, 5.22 ZnO, 6.38 H_2O		2 failed in 45 hrs.	None failed in 48 hrs.
5	76.36 Zn, 14.74 Al, 7.60 Cu, 0.36 Sn, 0.27 Si, 0.55 Fe	405–365	None failed in 48 hrs.	None failed in 48 hrs.
6	80.52 Zn, 18.35 Sn, 0.28 Fe, 0.70 Al	375–360	2 failed in 2 hrs., 1 failed in $16^1/_2$ hrs.	2 failed in $1^1/_4$ hr., 1 failed in 17 hrs.
7	98.62 Sn, 0.56 Bi, 0.40 Fe + Al, 0.17 Zn	230–210	3 failed in 45 hrs.	None failed in 48 hrs.
8	44.98 Sn, 35.44 Zn, 14.68 Cd	145–130	3 failed in 45–48 hrs.	2 failed in 48 hrs.
9	35.78 Sn, 9.33 Al, 0.60 Cu, rem. Zn	330–270	2 failed in $2^1/_2$–$4^1/_2$ hrs., 1 failed in 45 hrs.	2 failed in 44 hrs., 1 failed in 48 hrs.
10	63.86 Bi, 35.89 Sn, trace Fe	150–135	3 failed in 1–$1^1/_2$ hrs.	3 failed in 20 min.

Hard soldered joints (1:7.94 Zn, rem. Al, 2:8% Mg, rem. Al) in Al plate were tested in distilled water and tap water containing 5% NaCl, both at 93° C., and in conc. acetic acid at room temperature. The Al-Zn joint was heavily corroded in the NaCl solution, to which the Mg-Al joint was resistant. The latter was, however, rapidly corroded in conc. HNO_3. The corrosion resistance of joints made with a hard solder containing 11.5% Si, rem. Al, was satisfactory in all reagents. The opinion is expressed that differences in electrode potential, as ordinarily measured, are less important in the corrosion of soldered joints than the nature of the surface and microstructure of the joint.

1928—Rostosky and Lüder[325]

Aluminum sheets 2.4 x 3.2 x 0.12 inch thick were butt soldered with four aluminum-base solders containing 74 to 88% Al and 3 to 5 other constituents (not named). Two of the solders contained a considerable amount of silicon; the other two contained practically none. The joints were immersed in sea water (1 to 19° C.) for 6 months. Micro-examination of the corroded silicon-free specimens showed that sea water penetrated deep along the fusion zone, corrosion starting at the joint and spreading out therefrom. The sea water simply blackened the joints containing silicon, and formed a scale which protected the metal underneath. The results, of course, apply only to corrosion by chlorine cations.

Corrosion in Brewery.—Butt soldered joints and a soldered shell with a number of joints (same solders as above) were exposed for $13^1/_2$ months: (1) to humid CO_2 air of brewery cellar; (2) in a beer vat so that one-half of the joint was immersed in beer, the other half exposed to CO_2 air; (3) in a storage tank for light beer; (4) in service (shells) for storage of yeast. No corrosion was observed in any joint.

1927—Evans[326]

Electrode potential measurements were made on 6 solders against a soft aluminum sheet containing 0.40 Fe, 0.33 Si, ground with emery. The composition of the solders is shown in Table 50.

Table 50—Composition of Solders Tested by Evans[326]

No.	Pb	Sn	Zn	Cd	Cu	Al
1	50	50	..	..	..	..
2	35	35	30	..	..	..
3	..	..	75	20	..	5
4	..	78	8	5	..	9
5	..	83	10	..	..	7
6	..	81	..	..	3	16

Solder No. 1 in 0.1 *N* Na_2SO_4 was cathodic but the current fell within a few minutes to 0.001 ma. per cm.^{2} of Al, which was negligible. In 0.1 *N* NaCl the solder was also cathodic, the current being greater and depending on the supply of oxygen to the solder cathode. Solders 2, 3, 4 and 5 were normally anodic to aluminum. Vigorous abrasion of the Al reversed the polarity, but only for a few seconds. Solder No. 6 in 0.1 *N* NaCl was at first cathodic, but soon the polarity depended on the treatment of the electrodes. Evans concludes that the measurement of "equilibrium electrode potentials" is of little value because the potential rapidly moves from equilibrium when current is flowing. He points out that the belief that solders should be cathodic toward Al does not take account of the oxide film ordinarily present on Al. The addition of Zn to Al solder to make the solder anodic is not considered good practice unless the joint is to be subjected to cathodic corrosion.

Aluminum strips with holes filled with plugs or "blobs" of solder were immersed horizontally in dishes containing: 0.1 *N* NaCl, Cambridge (England) tap water, or 0.1 *N* Na_2SO_4. After 3 days a circle of membranous corrosion product was conspicuous with Solders Nos. 2, 3 and 4. Bubbles were evolved from Nos. 3, 4, 5 and 6, showing that these solders are subject to corrosion of the hydrogen evolution type, even in the absence of oxygen. Solder No. 6 showed less attack than the definitely anodic solders, but was perceptibly attacked by 0.1 *N* NaCl. The cathodic solder No. 1 caused remarkably little attack on Al. However, in the use of cathodic solders, care must be taken to keep the area of the solder small and to avoid crevices between solder and Al.

Aluminum strips 4.2 x 0.8 inch were abraded with emery and prepared as shown in Table 51, and immersed $^2/_3$ of their lengths in 0.1 *N* NaCl at an angle of 70° to the horizontal for 21 days.

Table 51—Corrosion of Soldered Aluminum. Evans[326]

	Condition	Loss of Weight, Mg.
A	Blob of solder No. 1	3.3
B	Strip of solder No. 1 pressed in position top to bottom	26.9
C	Strip of solder No. 1 connected externally to Al	20.1
D	No solder	2.4

The A specimen had a blob of solder fused through a hole at the bottom of the strip forming two buttons 0.16 inch diam. Specimen B had a strip of solder 0.16 inch wide pressed into contact with the Al but without fusion, right down the front of the specimen and passing around the bottom to the back. Specimen A showed very little effect due to the presence of solder. In specimen B the Al was subjected to anodic attack on account of the large area of solder. Although appreciable corrosion occurred along the line of contact of Al and solder, much more serious pitting occurred along the water line, which is invariably the point at which the protective film breaks down on a metal subject to anodic attack. Actual contact between the solder and Al in the liquid is not essential to the heavy attack, as shown by Specimen C.

Evans states (no details) that hard soldered joints (Al base, silicon alloy) in Al are comparatively free from corrosion. A cell composed of the hard solder and Al tended to equipotential.

1927—Eyles[327]

The soldered joints in a drinking water tank made of hard rolled aluminum 0.048 inch thick failed in a few months. The solder contained 75% Sn, 7.5% Zn, 17% Al, 0.5% P (as 10% phosphor-tin) and appeared to have disintegrated. Practically all patented aluminum solders fail when exposed to moisture because: (1) The oxide film on Al prevents alloying; (2) The high thermal conductivity (twice that of steel) and specific heat (0.212, twice that of copper) of Al renders local heating to the alloying temperature tedious; (3) Aluminum is highly electro-positive to most solders.

1925—Anderson and Boyd[328]

A soft soldered Al joint was severely corroded after 68 hrs. in tap water.

1923—U. S. Bureau of Standards[329]

Strips of Al and Al alloy (no details) were cleaned and coated with 6 Al solders. The composition of five of the solders fell within the range: 62–86 Sn, 8–37 Zn, 0–11 Al, 0–8 Pb, 0–5 phosphor-tin, 0–2 Sb, 0–3 Cu. The sixth

contained 75 Zn, 20 Cd, 5 Al. All appeared to wet and joint thoroughly with Al. After the tinned strips had been immersed in water for 48 hrs., they showed blisters, 0.02 to 0.12 inch diam. which, upon being broken, indicated that the Al underneath had not alloyed with the solder. The blistering increased with time until a large proportion of the tinned layer could be stripped off. Extensive corrosion also took place, the Al adjacent to the tinned layer being rapidly attacked with formation of gelatinous Al_2O_3. The tin-base solders themselves were not corroded; the zinc-base solder was. The electrode potentials of the solders in the following solutions: 0.1% H_2SO_4, 0.001 *N* $Al_2(SO_4)_3$, and a solution containing 0.001 *N* $Al_2(SO_4)_3$ and 0.005 *N* HCl, with respect to Al ranged from −0.269 to −0.445 volt. The negative sign means that current flowed in the solution from Al to solder. Therefore, these solders accelerate the corrosion of Al.

1923—Bauer[330]

The electrode potentials of several Al solders with respect to Al measured at 18° C. after immersion for 120 hrs. in 1% NaCl ranged from −0.217 volt to −0.345 volt. The solders were submitted in a national competition (Germany). Corrosion tests in boiling tap water, 1% NaCl solution and saturated steam followed by bend tests were also performed. No details of compositions or results are given. The prize was eventually awarded to a flux, not to a solder.

General Observations on the Corrosion of Soldered Joints in Aluminum

Experimental results as well as service behavior[331] confirm the better corrosion resistance of hard soldered joints compared with soft soldered. Eyles,[332] Nischk,[333] and the U. S. Bureau of Standards[329] recommend the painting of soft soldered joints in Al, particularly in moist atmospheres. Bosshard[334] observed that when brazed joints are made in the galvanized steel core of Aldrey transmission lines, and the joints are subsequently tinned, corrosion of Aldrey in salt spray is greatly increased in the vicinity of the joint. Among hundreds of aluminum solders, one of the most recent is Alunize[391] (m. p. 160° C.) which is claimed to withstand deterioration (no details).

The Technical Institute of Stockholm[244] found that a soft solder for Al containing 85 Sn and 15 Al is not corrosion resistant, and Sterner-Rainer[335] states that soft soldered joints for cast Al should not be used in sea water.

Experimental Corrosion Tests of Soldered Joints in Metals Other Than Aluminum

1936—Willstrop, Sidery and Sutton[336] (Mainly Fluxes)

Corrosion tests (no details) on 18–8, 18 Cr–2 Ni, and 13 Cr stainless steels (cleaned with emery cloth or pickled anodically in a bath containing $^1/_3$ by vol. concd. H_2SO_4 and 1.5% potassium dichromate) soldered by means of 65 Sn–35 Pb showed that, of four fluxes used: borax, boric acid, ammonium benzoate, and di-basic ammonium phosphate, only the last was satisfactory.

Lap joints in the three steels mentioned above (pickled anodically except for 18–8 which was used in the bright-rolled condition) made with three solders: (1) 64–66 Sn, 1.0 Sb, rem. Pb; (2) 49–51 Sn, 2.5–3.0 Sb; rem. Pb; (3) pure Sn and two fluxes: orthophosphoric acid and $ZnCl_2$ were exposed in a laboratory, in the open air, and to a sea-water spray test for 6 to 14 months. If the joints were thoroughly washed, no corrosive effects were observed. Residues of flux, however, caused corrosion which was much more serious with $ZnCl_2$ than with orthophosphoric acid. With the latter, when a sufficient quantity of metal has dissolved, a protective coating of phosphate is formed. The intermittent sea-water spray tests caused a negligible amount of corrosion of the exposed solder. There was no noticeable difference in corrosion resistance between the solders. Appreciable rusting occurred only with the 13 Cr steel containing no nickel.

Soldered lap joints (65 Sn, 1 Sb, 34 Pb) in brass, mild steel, and 18–8 using $ZnCl_2$, orthophosphoric acid, and two experimental organic phosphate fluxes were exposed as soldered in an unheated building for 6 months. The results showed that the desirable properties of orthophosphoric acid can be secured in more convenient form by using it in more or less complete combination with organic bases, such as aniline, and in a convenient vehicle, such as mineral oil. Phosphoric acid alone forms protective films on the materials examined and similar films are probably produced in samples soldered with fluxes containing organic phosphates as active constituents.

1936—Burns[337]

The potential difference at room temperature of the couple: wiping solder—lead cable sheath is shown in Table 52. The solder is the anodic or corroding electrode.

Table 52—Couple: Wiping Solder—Lead Cable Sheath. Burns[337]

Solution	Potential Difference, Millivolts
0.01 Molar Potassium Chloride	6 ± 3
0.01 Molar Caustic Soda	11 ± 3
0.01 Molar Acetic Acid	20 ± 8

The potential differences are said to be too small to maintain sufficient flow of current in the soil solutions usually encountered. Some instances of severe pitting of the solder have been observed in service, however, where the electrolytes were somewhat alkaline and contained abnormally low concentrations of film-forming constituents, such as sulfates and organic colloids.

1935—U. S. Bureau of Standards[119]

Soft soldered joints in unstabilized and stabilized 18–8 were subjected to 20% NaCl spray for 500 hrs. at 35° C. Soft soldering did not cause intergranular corrosion in either type of 18–8 (no details).

1935—Joint Research Committee of the Institution of Gas Engineers and Leeds University.[273]

Soldered zinc, nickel and aluminum were subjected to flue gas containing 1 to 8 grains sulphur per 100 cu. ft., and 6.5 to 9.2% CO_2 for 6 weeks at 58° F. In no instance did soldering affect the results. With solder-coated copper, copper appeared in the condensate after 15 days, the lead being preferentially removed from the solder.

1934—Miethke and Witt[128]

Soldered tin was corroded by two brines: Reinhartin, 28° Bé, containing $CaCl_2.6H_2O$ and $MgCl_2.6H_2O$ in 1:3 ratio, with protective colloid and buffers; and Kühlsoleen, 22° Bé; but was not corroded by Kühlsoleen Special, 19° Bé (same as Reinhartin but containing a silicate sediment) nor by Frigisol, 22° Bé, $CaCl_2.6H_2O$ and $MgCl_2.6H_2O$ in 1:3 ratio with sodium chlorate as anti-corrosive. The specimens were immersed in the brines for 30 days at −5° C. (Original report not available to the reviewers.)

1934—Mohr, Kramer and Schröter[129]

Tests showed that soldered copper was fairly resistant

to a variety of milk products. (Original report not available to the reviewers.)

1934—Roux[338]

Roux suggests several rapid electrochemical tests for evaluating the corrosion resistance of soldered joints.

1932—Kosting[339]

Sheet electrolytic copper strips (16 ounce gage, 0.0215 inch thick) were dipped in a solder containing 50% Sn, 50% Pb by weight, using either a commercial, noncorroding flux or a flux made by neutralizing HCl (1.185 sp. gr) with Zn, filtering, and adding 10 gm./liter NH_4Cl. Although actual soldered joints were not tested, the dipped specimens duplicated to some extent the conditions existing in butt joints. Three methods of corrosion testing were employed.

1. Accelerated weathering test in industrial atmosphere simulated by gas containing 1% SO_2, 5% CO_2, 94% air by volume. The gas entered the corrosion vessel through the bottom, which was covered by water thermostatically controlled. The specimens were supported on a lead-coated Al platform. After weathering for 100 hrs. the specimens showed a very slight gain in weight; after 200 hrs. a very slight loss. Removal of the corrosion film by pickling created a considerable loss. The loss of weight method was not considered reliable.

2. Repeated immersion test using apparatus designed by Rawdon and coworkers. The specimens, which were supported on glass hooks were dipped in a solution (1 *N* NaCl plus 0.6 *N* H_2SO_3) for $^1/_2$ minute every 15 minutes.

3. Salt spray: 20% NaCl, 15 psi air pressure. Tests were performed for 200 hrs. at 40° C. and 50° C., and 90 hrs. at 60° C. The corroded specimens were subjected to tensile tests and to repeated bend tests (number of reversals under tensile fiber stresses of 1000 and 4000 psi).

Several kinds of corrosion were observed.

1. Furrowing. Corrosion is concentrated in a narrow zone at the junction between solder and copper. Kosting discovered that the Cu-Sn alloys formed at the edge of the soldered zone were brittle and prone to cracking. If the brittle layer is covered by ductile solder, the layer does not crack but at the boundary of the soldered zone the Cu-Sn is *unavoidably* exposed. The copper exposed by the crack forms the anode of an electrolytic cell with the Cu-Sn alloy in the presence of a suitable solution, and is preferentially corroded. As would be expected, furrowing occurred with both fluxes. However, furrowing was observed only in the weathering tests, not in the alternate immersion test. The intensity of furrowing increased with the temperature (poor workmen use high soldering temperatures), probably because the formation of Cu-Sn alloy is favored. Furrowing reduced the tensile strength of a specimen weathered for 200 hrs. at 50° C. by as much as 3000 psi, and reduced the bend value from 50 to 60 bends unweathered to 30 bends weathered (unannealed specimens).

2. Pitting. Pitting occurred first where the solder was thinnest. The copper at the bottom of the pits was covered with patina. The pits correspond to blow-holes in the solder.

3. Uniform corrosion. The repeated immersion specimens were covered with a uniform green film. Copper soldered at 270° C. and annealed at 50° C. was much more severely corroded than unannealed soldered specimens.

4. Flux corrosion. Flux forms insoluble copper salts which, if not removed, eventually cause pitting.

Kosting reviews the little that was known about his topic before 1932.

1931—Wilson[340]

Soldered brass automobile radiators were subjected to circulating solution tests for 1400 hrs., the coolants, which were tap water and water solutions of several anti-freeze compounds, being maintained at 190 to 200° F. (90° C.). With tap water the brass was perforated after 300 hrs., but the soldered joints showed little attack. Under service vibrations, however, the soldered joints failed before the brass itself. Anti-freeze compounds, being non-electrolytes, were actually beneficial to corrosion resistance.

1930—Kusenack[341]

The corrosion of solders in refrigerating brines is discussed. (Original article not available to the reviewers.)

1929—British Non-Ferrous Metals Research Association; Association Series No. 41.

Part 1 of this report contains results of corrosion tests on solders. (Report not available to reviewers.)

1927—Evans[326] (experimental details not given in full)

If the solder is anodic to the metal joined, corrosion will be concentrated on the solder. The degree of attack depends on current (potential difference) and film formation. Soft solder on copper in sulfate solution acquires a protective film as a result of anodic reaction. No film is formed in chloride solution. In 0.1 *N* NaCl the corrosion product of the solder may settle on the copper and, by screening the surface locally from oxygen, cause local anodic attack on the copper too.

A study of currents generated by contact between solder and iron showed that current may flow in either direction, depending on conditions, and is usually weak. In many cases, chlorides cause stronger and more persistent currents. Hence, fluxes containing the chlorine ion should not be used. Besides, chlorides of many metals (Al, Zn, Fe) are hygroscopic.

1926—Lassen and Ornitz[97]

Lassen cites soldered joints in salt brine or $CaCl_2$ refrigerating solution in which the solder swelled to 5 times its original size. Ornitz attributed the effect to HCl as cleaning agent prior to soldering, and stated that $CaCl_2$ brine should have no effect on soft solder unless the solder contains zinc, or unless electrolytic action can occur.

General Observations

Noyes[342] states that the Navy uses lead-tin solders for all applications involving fresh water or sea water. Tay,[179] however, believes that soldering or riveting is not satisfactory for stainless steel restaurant equipment, on account of the corrosion problem, and Waehlert[343] states that nickel and cupro-nickel may be soldered only when corrosion is not a factor. The corrosion resistance of aluminum soldered to brass is bad, according to Lüder.[344] Zibe[345] states that only gold parts having identical gold content should be soldered, otherwise corrosion of the local element type occurs. Ray[346] reports good erosion resistance of soldered strips on steam turbine blading, and a method of protecting soldered joints from corrosion by means of a special coating has been patented.[347]

Copper pipes (composition not stated) with streamlined caps and couplings (12 x 1.5 x 0.062 inch) soldered in place withstood immersion for two years in a tidal marsh without visible effect, according to Logan.[348]

According to Bregman,[348] brine solutions, such as NaCl or $CaCl_2$ free from $MgCl_2$, can be satisfactorily used in lead pipes with soldered joints, but there is danger of corrosion of soldered copper, the solder being galvanically corroded. Solder in contact with copper or

other metal is not attacked by anhydrous Freon, SO_2 or methyl chloride.

The effect of the composition of the solder on the corrosion resistance of soldered joints in steel is discussed by Thews.[349] Up to about 3.5 % Sb has no effect on the appearance and corrosion resistance of soldered joints. With higher Sb content, moist air may cause deterioration. Arsenic is harmful to corrosion resistance. Up to 3% Zn or Cu is also harmful. Over 0.05% Zn gives rise to oxide films, which reduce the corrosion resistance. Blain[350] found that zinc and aluminum as well may cause corrosion of electrical connections, if either is present in the solder. Antimony particularly, and to a less degree, Zn and Cu are harmful to the corrosion resistance of lead-tin solders, according to Barber,[351] who did not test soldered joints.

Montelucci[352] states that Cd-Zn eutectic solder is electropositive to iron and protects it against corrosive attack. According to an anonymous writer,[353] the cadmium soft solders have almost as good corrosion resistance as Pb-Sn solders. The corrosion resistance of soldered joints using Pb-Sn-Cd solder is said to be improved by adding 1 to 3% Zn to the solder.

Pure tin or tin containing 3% antimony, according to Daniels and Macnaughton[386] should be used for soldered joints exposed to corrosive cooling media, such as brines or SO_2, which are likely to disintegrate lead-tin solder.

The rate of corrosion of solder containing 68% Pb, 23% Sn, 9% Cd (not a joint) totally immersed in saturated lime water, distilled water, or 0.001 *N* acetic acid was found by Schumacher[354] to be the same as for standard Pb-Sn solder at 25° C. The samples had a total area of 138 square inches. The composition of the solder has no effect on the corrosion of soldered joints in copper, according to Miller,[355] who regards the flux as the determining factor.

Hanemann[403] describes a method for estimating the effects of corrosion in tap water on soldered lap joints by subjecting the corroded specimen to a tension test. He gives no results.

Silver Soldered Joints

There appears to be no information on the corrosion resistance of silver soldered joints. Leach[356] states that silver solders are resistant to ordinary atmospheric corrosion, but are attacked by HNO_3 and are discolored by gases or products containing sulfur. Where Zn is harmful to corrosion resistance the Ag-Cu eutectic solder is employed. According to McKay,[357] corrosion may be localized at silver soldered joints by concentration cells or galvanic differences. The best protection from galvanic attack, which is roughly proportional to the ratio of the areas involved, is obtained by using a cathodic solder. Silver soiders are cathodic to Monel Metal and to Ni-Cr-Fe alloys under many conditions. They are anodic to the latter in concd. HNO_3, however. E. A. Smith[388] points out that the corrosion resistance of silver solders depends on silver content and Cu-Zn ratio (best at 60:40). The addition of 2% Ni is said to raise the corrosion resistance of silver solders. Snelling and Thews[359] also recommend the addition of nickel and state that overheating damages the corrosion resistance of silver solders.

Snelling[390] has reported results of salt-spray tests on silver soldered 18–8 but the original article was not available to the reviewers. The effect of additions of cadmium, iron and lead to the solder was studied. Silver solders are resistant to many of the reagents to which high nickel alloys are resistant, according to Flocke and Schoener.[360] Bassett[389] states that all silver solders are attacked by HNO_3 and discolored by sulphur. The corrosion resistance of joints should be determined by trial. According to Hatfield,[404] contact with silver solder increases the corrosion of stainless steel containing 14% Cr.

The Effect of Flux on the Corrosion Resistance on Soldered Joints

As pointed out by Butters,[361] Blain[350] and several of the experimental investigators, corrosive soldering fluxes for copper and other metals are sources of failure. The effect of flux is also shown by some experiments reported by Abraham,[362] who tested soft soldered loop joints of steel cables for 120 days in salt spray. The cables were of three materials: galvanized steel, cadmium plated steel and 18–8. The joints were soaked for 24 hrs. in soda water to remove traces of soldering acid, but the treatment was ineffective. The effect of the acid (HCl) was pronounced on bare wires, particularly 18–8. The salt spray caused losses of strength up to 21%. A protective oil coating had negligible effect, but aluminum paint reduced the loss of strength due to corrosion to 2.5 to 4.4%. The soldered joints compared well with other types of joints. According to Thews,[349] fluxes containing chlorides accelerate the corrosion of all soldered joints, particularly those containing zinc, or those containing blow holes or other crevices. A cyanide or alcohol wash or hot water wipe is recommended by Falk[363] to remove flux (NH_4Cl plus $ZnCl_2$) from soldered seams on copper and galvanized iron. The International Tin Research and Development Council recommended that soldered joints for outdoor exposure be wiped free of flux with a cloth soaked in a solution of washing soda, to prevent subsequent corrosion.

Corrosion Fatigue

The literature on the corrosion fatigue resistance of welds has been summarized in "Fatigue Strength of Welded Joints."

Subsequent to the publication of the Fatigue review, Stewart[364] has described the corrosion fatigue test performed by the U. S. Naval Inspector on test plates for process approval of welding for Class A-1 pressure vessels for the Navy. Rotating cantilever fatigue specimens are cut from the weld metal in plates 16 x 12 inches. The specimens are conically tapered over a length of 2 inches, the diameter at the section of maximum stress being 0.46 inch. The radius of fillets is $1^3/_{16}$ inch and the surface is polished as for micrographic examination at 100 diameters magnification, the polishing being alternately transverse and longitudinal. The corrosion fatigue test is performed at 1450 r.p.m. in deep-well fresh water enveloping the entire tapered section. The results for welded boiler steel are shown in Table 53. The mineral oil was non-corrosive. Semi-logarithmic plots were horizontal for all specimens in air (mineral oil) at 10×10^6 cycles but were not horizontal for welded or unwelded specimens at 50×10^6 cycles. The value given in the table is that shown at 50×10^6 cycles.

Table 53—Corrosion Fatigue Tests of Welded Boiler Steel. Stewart[364]

Specimen	Fatigue Limit, psi: Specimen in Mineral Oil	Fatigue Limit, psi: Specimen in Fresh Water
Base metal	24,000	19,000
Welded plate	24,000	18,000 to 19,000

A brief review of the corrosion fatigue of welds is given by Thum and Ochs,[379] who report results by Lipp on small T-welded mild steel specimens in reversed

bending. All specimens broke outside the weld, which showed no attack although the rolled mild steel base metal was pitted (no details).

The Corrosion Resistance of Flame-Cut Surfaces

Melhardt[365] immersed flame-cut and planed specimens of mild steel 1 inch thick weighing 90 gm. in an aqueous solution containing 1% H_2SO_4 by volume. After 44 days the flame cut specimen weighed 72 gm., the planed weighed only 68 gm.

Oxy-cut and planed specimens of mild steel (0.17 C) were also tested by Wiss[366] and Dehasse.[367] In 1% H_2SO_4 the loss of weight due to corrosion was 30% higher for planed than for oxy-cut specimens. Results of other tests are given in Table 54. The figures are loss of weight

Table 54—Corrosion of Oxy-Cut and Planed Mild Steel. Wiss[366]

Dimensions of Specimen—Inch	Running Tap Water 206 Days		1% NaCl Solution 158 Days	
	Planed	Oxy-Cut	Planed	Oxy-Cut
0.4 x 0.4 x 1.6	851	813	753	606
0.4 x 1.0 x 1.6	1709	1270	1132	925

in milligrams during continuous immersion. After 12 days in either reagent the flame-cut surfaces lost more weight than the planed. After longer tests the loss of weight of the planed specimens exceeded that of the flame cut. Wiss[368] also states that flame cutting nickel steels (20 to 35% Ni) actually improves their corrosion resistance.

The superior corrosion resistance of flame-cut surfaces may perhaps be explained by the oxide coating and by the smaller specific surface exposed, compared with furrowed, planed surfaces.

Corrosion of Welds at Elevated Temperatures

Stockton[380] cites several instances. A small arc-welded mild steel carburizing box (27 x 12 x 8½ inches) was used for over 500 hrs. at 900–930° C. The weld metal appeared to have been as resistant to scaling as base metal. Experience with annealing pots for several years, according to Sarazin,[24] showed that welds made with ductile electrodes had better scale resistance than base metal, the welds remaining in relief on the pots after service. No trouble with excessive scaling of welds in welded annealing pots or annealing pots with welded cracks was observed by Caldwell[381] and Hengstenberg.[382] Cramp[369] also states that arc welded furnace rollers and other parts have good scale resistance. An anonymous writer[158] states that the presence of nickel in filler rods for oxyacetylene welding plain-chromium steels lowers the resistance to high-temperature oxidation. The U. S. Navy sometimes specifies that welds in 4–6% Cr- 0.5% Mo steel must not show greater scaling than base metal after 1 hr. at 1200° F. (650° C.). A similar requirement is made of welds in 25 Cr-20 Ni steel after 1 hr. at 2200° F. (1200° C.).

The attack of welded joints by hot, high-pressure hydrogen is dealt with by Gross,[46] Inglis and Andrews[370] and Schottky.[371] Gross states that high-temperature hydrogen attack in hydrogenation apparatus is more pronounced the more the weld metal differs in composition from base metal. Discussing hydrogen attack at 250 atm. 300 to 320° C., Inglis and Andrews[370] show that a heat-treated steel containing 0.28 C, 0.70 Cr, 3.38 Ni, 0.21 Si, 0.38 Mo was not attacked after 8000 hrs. However, a specimen having a light surface fillet weld fissured and decarburized badly after 5000 hrs. The slightly coarsened microstructure at the weld explained the sensitivity to attack and suggested the remedy: re-heat treatment after welding. According to Schottky,[371] welds in Cr-Mo steel tubes made with medium-Mn filler rod are particularly subject to decarburization and embrittlement by hot, high pressure hydrogen, on account of porosity and defective penetration.

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THE ENGINEERING FOUNDATION
WELDING RESEARCH COMMITTEE

Sponsored by American Welding Society and American Institute of Electrical Engineers

Supplement to the Journal of the American Welding Society, September 1937

Translations

Translations of the following articles are available for loan only.

1. E. Höhn's book on Boiler Welding, published in Berlin, 1935. Sections dealing with multi-axial stress.

2. The Theory of the Web Plate Stiffened on one Side, by E. Chwalla and A. Novak, published in *Stahlbau*, **10** (1937) 73–76, 92–96.

3. Fundamental Ideas on the Resistance of the Web Plates of Plate Girders to Buckling, by Dr. Krabbe, published in *Stahlbau*, **10** (1937) 97–100.

Since only two or three copies of each translation are available, they will have to be returned within a short time after receipt by any one requesting them.

Weld Failures!

Information from practical welders or welding engineers as to details of weld failures due to cracks which have come under their personal observation will be greatly appreciated by Prof. M. F. Sayre of Union College, Schenectady, N. Y. This applies both to cracks which appear while the welding is going on and to later failures in service both in steel and in non-ferrous metals. A brief description, which would include the composition of the material, the manner in which the welding was done, the time at which the cracks occurred, their direction and location relative to the welding seam and a pencil sketch of the piece near the weld, or as much of this information as could be easily jotted down, would be most valuable.

This information is desired only in connection with studies sponsored by the Welding Research Committee, made with a view to the formulation of a possible program of future experimental work on the causes and prevention of cracks, and in particular the relationship between residual shrinkage stresses and cracks.

A Summary of Fatigue Strength of Welded Joints

German Engineers have on a number of occasions commented favorably on the Critical Reviews of Literature prepared by the Welding Research Committee. A translation of the summary on fatigue strength has been made by Dr. H. Cornelius (Berlin-Adlershof and published in *Z. VDI* (Journal of German Society of Engineers) **81**, No. 30, Pages 883 to 888, July 24, 1937.

The introduction to Dr. Cornelius' summary states:

"The AMERICAN WELDING SOCIETY has undertaken to critically review recent foreign and domestic welding literature. One of the results is a review to October 1, 1936, on the fatigue strength of welded joints. The most important conclusions arrived at by the reviewers are summarized in the following article."

Preliminary Report of Subcommittee on Weld Stresses*

The problem of the serviceability of a welded structure that has been preloaded by the temperature stresses incident to its fabrication is more serious than is apparent at first thought. The seriousness of this problem lies in the interrelation of two facts: (1) brittle failures have been noted in steels, otherwise ductile, under bi-axial tension loading; (2) residual stresses, in large areas, caused by temperature gradients are bi-axial in nature.

Stress-relief after fabrication removes such classifications from consideration under this problem. Large structures such as ship hulls are being welded that cannot be corrected in a furnace before admission to service. The importance of the problem in those cases has already been demonstrated. This committee herewith undertakes to state the problem, suggest lines of attack and to outline basic experiments that should delineate the dividing line between safe and dangerous practices.

The two components of the problem and their relation can be stated as follows:

I. Biaxial properties of steel.

(*a*) How can brittle failures be produced in an ordinarily ductile material?

(*b*) Within what range of stress values will brittle failure occur?

II. Production of bi-axial stresses by temperature gradients.

(*a*) In simple configurations such as a circular disk, what is the relation between temperature gradients and resultant stresses?

(*b*) Within what range of variables are these stresses residual?

A. Correlate these two investigations to define:

1. What temperature gradients are dangerous?
2. At various temperature gradients how much usable strength is available for the service load?

The problem should be attacked from four angles:

(1) Library Research
(2) Survey of Industrial Case Histories
(3) Development of Methods of Measurement
(4) Pertinent Experiments.

These four attacks are outlined as follows:

(1) Library Research

An intensive library research should be initiated with a view to uncovering the pertinent researches in the field of heat conduction, with special reference to transients, temperature distribution in time and space, temperature gradients, temperature stresses, residual stresses, the bi-axial properties of steel and, in particular, authentic methods of measuring the above-mentioned subjects.

(2) Case Histories

A collection should be made of various case histories of failures which seemingly are attributable to bi-axial loading of steel. Properly presented in a questionnaire, many industrial plants could report troubles which are directly attributable to either bi-axial stresses or residual stresses.

These reports would be of immense interest to the various staffs working on the problem. An even more important use would be to publicize the problem using these reports as the meat. A few typical cases follow:

Cold rolled steel cannot be successfully machined without a severe change of shape. One member reports a case in which cold rolled steel disks which had theoretically been annealed before using were slit radially to provide certain anchorages. The disk itself upon initial slitting distorted considerably, but what

* Under consideration by the Industrial Research Division but not yet approved for execution. Comments invited.

was most interesting was the fact that the distortion, after a year of service, was considerably aggravated by the service in which it was used. Undoubtedly the temperatures that obtain in a clutch were sufficient to promote the creep of the fabrication stresses.

Another example occurs in the bending of thick plates to form the shells of pressure vessels. The rigidity of the plate adjacent to the bend line inhibits the lateral contraction and expansion (Poisson's Ratio) at the bend line to a point where an ordinary good mild steel must not be rolled to an arc corresponding to more than 5% change in fiber length if it is not to fail in the bending operation. This is a phenomena distinctly descriptive of the lack of ductility exhibited by steel when it is subjected to a two-dimensional tension load.

Another case of failure due to lack of ductility under the same type of load occurred in the case of an injector nozzle for a diesel engine. This case presents a heat treated tip forming the end of this small pressure vessel subjected to hydrostatic impact by virtue of the inherent nature of its service. These cups would break off quite regularly after a certain number of hours of operation, and it was impossible to attribute this failure to the usual fatigue phenomena involving stress concentrations.

Another field in which the bi-axial loading is extremely common—and probably one from which a great deal of background can be drawn—is the ordinary cupping operation of thin steel plates, which is so common in the automobile industry. Here is a phenomena in which the bi-axial ductility of steel is of great importance. A critical examination of the criteria describing a steel which will not draw would also prove valuable in its delineation of the other side of the problem. The nature of the phenomena also suggests a type of experimental attack which will be dealt with under Section Four of this report.

Certain experiments have also been done with steel tubes under an axial load, plus a circumferential load due to hydrostatic pressure. The report of this work should be located and reviewed. These tubes showed glass-like failures under the double loading, while under the single loading they exhibited a high degree of ductility.

A number of case histories similar to the above, properly presented and circulated among the universities, together with a statement of the problem before this committee, would serve as an active stimulant to interest various university staffs in this problem. It is strongly recommended that case histories of failures be studied and publicized. A failure is infinitely more instructive than a success, since it traces a definite path into the realm of the unworkable, while successes may be based on conservatism.

(3) Methods of Measurement

A great deal of preliminary work must be done to develop measuring apparatus and measuring methods for dealing with the complicated phenomena of temperature gradients, stress gradients and bi-axial properties of steel.

These method developments could adequately be farmed out to universities as a separate project, or could be considered a part of basic experiments themselves. The former might lead to final results quicker than if the method development was mixed up with actual experiments.

For instance, the subject of short gage length extensometers of a reliable nature is one which is not entirely solved. Some attention might be paid to the development of an instrument which would measure differences of stress or stress gradients.

Methods should be developed to measure the existing residual stresses that occur in a steel configuration of any sort. Undoubtedly, the relaxation methods would be the most fruitful field for this development, provided they were accompanied by careful mathematical analysis to establish their legitimacy.

The method for measuring rates of heat conduction in order to establish temperature gradients, consists of coating certain steel shapes with a wax of a definite melting point, applying a certain B. t. u. input at some spot in the plate and taking moving pictures of the melting edge of the wax. This would give quite definitely the temperature at any point in the plate at any time, and movies taken of the behavior of a successive series of waxes of different melting points would completely describe the heat transients in a certain configuration, as against a certain B. t. u. input. This procedure, in the simple cases, accompanied by mathematical analysis, would establish the corresponding stress gradients.

(4) Pertinent Experiments

(*a*) Bi-axial properties of steel

It seems that one of the first things to do in order to exactly delineate the dividing line between safe and dangerous stress gradients would be to define the properties of certain mild steels under bi-axial loading.

The ductile properties of steel as influenced by bi-axial tension should be one of the first to receive attention, since this is the property that seems to suffer the most under this type of loading, and at the same time is the property that the structure depends upon for adjustment. A good piece of work was done on the elastic constants below the yield point at the University of Illinois by Becker, but the constants above the yield point have not been systematically surveyed. This property should be investigated, not only at room temperature, but at various elevated temperatures in order that it may tie in with the phenomena under discussion.

Fatigue is another property which should be investigated under this type of loading, and this research will undoubtedly have to be preceded by development of a fatigue machine for bi-axial tension. Much work has been done in the field of fatigue on combined stresses. This work should be extended to completely survey fatigue properties when the two stresses are tension stresses, and at right angles.

The cupping of steel suggests a method whereby two-dimensional deformation of a known value could be established in thin steel plates and these could adequately form fatigue samples or ductility samples for the investigation of the actual properties of steel as affected by the presence of residual stresses.

(*b*) Thermal transients and resulting residual stresses

A mathematical analysis of heat transients, and the resulting residual stresses, could be checked by taking moving pictures of a photo-elastic set-up in which the specimen was strained by the application of a finite amount of heat at some point within its boundary. Undoubtedly the experiment would fall into trouble when the range of values were such that plastic deformations occurred, but the establishment of the values at which plastic sets occurred by working through the elastic range up to these points would be valuable. In material of certain conductivity coefficients and certain elastic coefficients, the temperature gradients necessary to exceed the yield point would be of interest in checking the behavior of steel.

The same basic experiment in steel could be executed by subjecting steel disks in a relaxed or unstressed condition, to various B. t. u. inputs at their centers. Measure the resulting thermal gradient, stress gradient and deformation of these disks. This would tie into the photo-elastic study mentioned above in that the heat inputs would be so chosen that part of the disks would suffer phenomena below the yield point and part of them phenomena above the yield point. The relaxation of the disks after cutting out a hole in the center might also give pertinent information from the relaxation view-point about the stresses that existed.

An experiment to establish values of relative rigidity necessary to produce residual stresses consists of heating a plug which was initially tightly fitted into a hole in a doughnut shaped piece of steel. At certain heat inputs and certain relative stiffnesses, this plug would undoubtedly take plastic sets which—when temperature equilibrium was finally established—would result in a loosely-fitting plug where it was initially tight. This experiment is the inverse of shrinking a locomotive tire on a driving center. The difference of temperatures to establish a plastic set in the plug would be pertinent information.

It is the intent of this committee to organize the work as outlined, steer the various researches and review results.

Everett Chapman, *Chairman*
R. Eksergian
A. B. Kinzel
A. Nadai
M. F. Sayre
J. Winlock

Report of Subcommittee of Non-Destructive Testing*

Organization:

The personnel of this subcommittee has not been entirely selected. It is expected, however, that this will be completed shortly.

Program:

The first subject selected for the consideration of this subcommittee is the correlation of radiographic tests and mechanical tests of welded joints. A questionnaire submitted to a considerable number of those in the welding industry has revealed the fact that such information would be desirable but has shown plainly that there is very little material available at present upon which such a correlation could be based. Accordingly, a tentative research program has been set up for the consideration of the committee. It is to be remembered that many phases of this work involve differences of opinion as regards the proper procedure as, for instance, the particular type of mechanical tests to be used in the correlation and these points must be considered in detail before experimental work is begun. The program presented here is merely a general outline of the proposed work.

General Program

1. Type of test sample
   - A. Butt or fillet weld
     - (*a*) Thickness
     - (*b*) Material
     - (*c*) Kind of welding.
   - B. Mechanical tests desired
     - (*a*) Tensile tests
     - (*b*) Bend tests
     - (*c*) Impact tests
     - (*d*) Type of specimen for these tests.
   - C. Radiographic Technique
     - (*a*) Choice of a standard procedure conforming to industrial practice.
2. Preparation of test samples
   - A. Specifications based on decisions of Section 1
   - B. Methods of obtaining desired defects
   - C. Sources of the welded samples
3. Radiography of test samples
   - A. Maintenance of standard technique
   - B. Choice of laboratories.
4. Classification of radiographs
   - A. Methods of classification
     - (*a*) Type, size and distribution of defects.
5. Mechanical test of welded samples
   - A. Preparation of test specimens
   - B. Choice of laboratories for tests
6. Correlation of results

Special Program

One phase of the research program seems sufficiently developed so that experimental work can be started immediately. This is related to Section 4 of the General Program and is an examination of a method of classifying radiographs of welds in a quantitative fashion.

The dark areas on a radiograph, which are indications of defects in the interior, are due to the reduction in thickness of the metal caused by these defects. The density or blackening of the radiograph at any point can be directly measured and is definitely related to the thickness of metal at this point. Thus, a density survey of the radiograph could be translated into a thickness survey of the weld itself, and an integration of the thickness across any section would supply a measure of at least one factor contributing to the strength of the weld.

The investigation would consist of the following steps:

1. Development of a suitable recording densitometer. (This is well underway already.)
2. Determine the density-thickness relationship for various experimental conditions.
3. Apply the method to examples of typical welds.
4. Attempt to correlate results with mechanical properties.

Sufficient work has been done to indicate that this method is capable of giving a measure of the effective area of sound metal and of the general size and distribution of the defects. Some such method will be needed in carrying out the general program and no other is known at the present time.

It is intended that this special program be carried out at the Massachusetts Institute of Technology by graduate students during the next year under the direction of Prof. J. T. Norton.

JOHN T. NORTON

July 6, 1937,

* Under consideration by the Industrial Research Division but not yet approved for execution. Comments invited.

Resistance Welding Research Planned on Copper Alloys

The Material Committee on Copper Alloys of the Industrial Research Division, of which Colonel G. F. Jenks is Chairman, has announced that it is planning to investigate certain aspects of spot welding of copper base alloys. Among the specific problems suggested for research are:

(1) The effect of electrical resistance of the electrode material on the welding of copper and copper alloys.

(2) Determination of the cause of burning through of spot-welded plates of high zinc content alloys.

(3) Tabulations of the general spot welding characteristics of the more important copper base alloys.

(4) Relationship between the ordinary welding characteristics and such fundamental factors as actual energy applied, rate of dissipation of heat, etc.

(5) The measurement of surface contact resistances, both at room temperature and under conditions prevailing at the instant of actual fusion.

(6) Causes of various types of unsoundness in spot welds.

(7) Development of methods of test to be applied to rating of spot welds.

(8) Study of instantaneous temperatures attained at various points and correlation with other fundamental variables.

Under the auspices of this subcommittee, a paper on General Spot Welding Characteristics of Copper Alloys is to be presented at the Welding Society meeting in October.

The Effect of Carbon and Manganese on the Weldability of Plain Carbon Steels

This program of research is now being considered by the Industrial Research Division

Scope

1—The study of the heat effects produced by fusion welding or gas cutting on the base metal adjacent to the weld as revealed by changes in hardness and toughness and tendency to cracking.

2—To determine the physical properties of the welded joint and the parent plate in steels of varying carbon and manganese content.

Material for Testing

Manufacture.—The steel shall be made by the electric arc furnace process. All the heats are to be poured from the same furnace.

Chemical:

In order to perform the work of this research, the materials listed in Table 1 are desired:

Table 1—Carbon and Manganese

Carbon	Manganese				
0.10–0.13	0.30–0.40	0.60–0.75	0.90–1.10	1.20–1.40	1.50–1.70
0.20–0.24	0.30–0.40	0.60–0.75	0.90–1.10	1.20–1.40	1.50–1.70
0.30–0.35	0.30–0.40	0.60–0.75	0.90–1.10	1.20–1.40	1.50–1.70
0.40–0.45	0.30–0.40	0.60–0.75	0.90–1.10	1.20–1.40	1.50–1.70

(*Continued on page 32*)

Temperature Distribution During Welding
A Review of the Literature to January 1, 1937

By W. SPRARAGEN* and G. E. CLAUSSEN**

This Report Is Prepared Under the Auspices of the Literature Division of The Engineering Foundation
Welding Research Committee

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SUMMARY

In gas welds maximum temperatures of 1000° C. are reached in the center of 0.39-inch thick plate 3/4 inch from the weld, which drop off to 300° C. 3 inches from weld. The surface is about 150° cooler (namely, 850° C.) at 1 inch distance and only slightly cooler (240° C.) than the center at 3 inches. In plates half as thick the temperature 1 inch from the weld at the surface is only 450° C., and 150° C. at 3 inches.

In arc welds (covered electrodes) the temperature of 1000° C. is only obtainable at less than 1/4 inch from the center of the weld. There seems to be little variation in temperatures between the surface and the center, or between 0.20-inch plate and 0.39-inch plate. The temperature gradient is much steeper, the affected zone much narrower and the cooling from any given temperature is quicker in the arc welds as compared with the gas welds.

It would therefore seem reasonable to expect that stress gradients due to thermal reactions would be greater in arc welds than in gas welds. In steels where quick quenching action after welding would be desirable arc welding might be advantageous and where slow cooling is desirable gas welding would find favor. The heat-affected zone would be narrower and closer to the weld in arc welding, and broader and further away in gas welding.

The advantages of gas welding in these respects decrease with increased thickness of plates unless accompanied by preheating.

In copper and aluminum welding the temperature gradients are very flat when welding by gas.

Temperature Distribution During Welding

Experimental

Temperature Distribution

A comprehensive investigation of the distribution of temperature in a joint during welding has been made by Portevin and Séférian.[1] Their specimens consisted of plates and bars of mild steel, copper and aluminum welded by oxyacetylene or with covered electrodes. The plates were 20 inches square, 0.20 or 0.39 inch thick. The bars were square (0.39 inch) or round (0.20 or 0.39 inch diameter). Temperature was measured by means of platinum/platinum—10% rhodium thermocouples (0.008 inch diameter wire) protected by silica tubing and either spot welded to the under surface or inserted in holes, 0.08 inch diameter, drilled half way through the plates or square bars. Temperature was photographically recorded continuously by means of a mirror galvanometer. Welds in the plates were made in the central 8-inch portion, the couples being aligned (no details) perpendicular to the middle of the weld.

Oxyacetylene welds in mild steel were made with a torch delivering 90 cu. ft. acetylene per hour per inch of thickness. The average velocity of welding was

49 inches per hour, fore-hand welding, 0.39 inch plate.
63 inches per hour, back-hand welding, 0.39 inch plate.
98 inches per hour, fore-hand welding, 0.20 inch plate.

The maximum temperature attained during welding is shown in Fig. 1.

Arc welds in 0.39-inch plates were made in three layers with covered mild steel electrodes, 0.16 inch diameter for root layer (115 amps., 18 volts) 0.13 inch diameter for other layers (90 amps., 19 volts). The covering contained 58% Fe_2O_3, 31% $K_2O{\cdot}3SiO_2$, 5.20% SiO_2, 5% Al_2O_3, remainder CO_2, and H_2O. Figure 2 shows the maximum temperatures attained during welding.

The maximum cooling velocity at all points in the vicinity of the oxyacetylene and arc welds in mild steel is shown in Fig. 3. The temperature fields around the oxyacetylene and arc welds in mild steel plates 0.39 inch thick are shown in Figs. 4 and 5, respectively. The source of welding heat, arc or torch, is moving in the direction of the arrow and has just reached the point 0. The metal in front of the torch is subjected to the

* Secretary, Welding Research Committee
** Research Assistant, Welding Research Committee

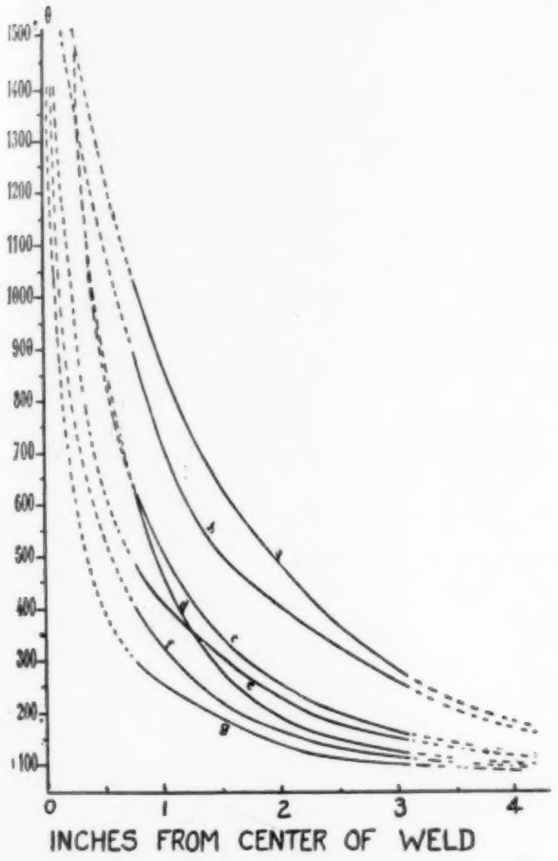

Fig. 1—Maximum Temperature Reached Along a Line Perpendicular to a Forehand Oxyacetylene Weld in Mild Steel

θ = temperature in ° C.
(*a*) 0.39 inch plates, couples at mid-thickness
(*b*) 0.39 inch plates, couples on surface
(*c*) 0.39 inch square bars, couples at mid-thickness
(*d*) 0.20 inch plates, couples on surface
(*e*) 0.39 inch square bars, couples on surface
(*f*) 0.39 inch diameter round bars, couples on surface
(*g*) 0.30 inch diameter round bars, couples on surface. Portevin and Séférian[1]

steepest, almost linear, temperature gradient. The finished weld cools at a slower rate. The curve $\theta_m = f(x, y)$ shows the maximum temperature obtained at any section beyond the weld. Curve $x = \phi(y)$ shows the location of these maximum temperatures when the source of welding heat is passing point 0. Curves $\theta = f_o(y)$, $f_s(y)$, etc., are the temperatures at sections beyond the weld and parallel thereto. The subscript indicates the distance (in centimeters) of the section from the weld. Figure 3 was derived from Figs. 4 and 5.

Oxyacetylene welds (forehand) in completely deoxidized pure copper (0.39 inch thick) were made with three torches, 70 to 90 cu. ft./hr. capacity. One torch was used to preheat, the required temperature being 500° C. Another torch was used for welding and a third torch for reheating. The torches advanced at the average rate of 40 inches per hour and gave rise to the maximum temperature distribution shown in Fig. 6. The corresponding surface of temperature distribution contains inflections owing to the preheating torch.

The maximum temperature distribution for forehand oxyacetylene welds in aluminum is shown in Fig. 7. The torch capacity was 70 cu. ft. per hr., the preheating torch raising the scarves to 300–350° C. The torches advanced at the average rate of 40 inches per hr. The time-temperature curve at a given point has the same shape as the distance-temperature curve for the section parallel to the weld containing the point under consideration provided the velocity of the heat source is constant and end-effects are neglected. The conversion factor between time and distance is $X = VT$, that is, the time scale must be multiplied by V, the velocity of the electrode, considering $T = 0$ when the temperature is a maximum.

The main factors affecting temperature distribution, the investigators conclude, are:

(1) the welding process, arc giving steeper temperature gradients but smaller heat-affected zone than oxyacetylene. There appeared to be no material difference between forehand and backhand methods.

(2) the thermal constants of the metal.

(3) the preheating temperature.

(4) dimensions and shape of parts to be welded.

It is unfortunate that many experimental details (period of galvanometer, interval between layers, etc.) are not described by Portevin and Séférian, for their's appears to be the most extensive and useful investigation of the subject. It has not been considered worth-

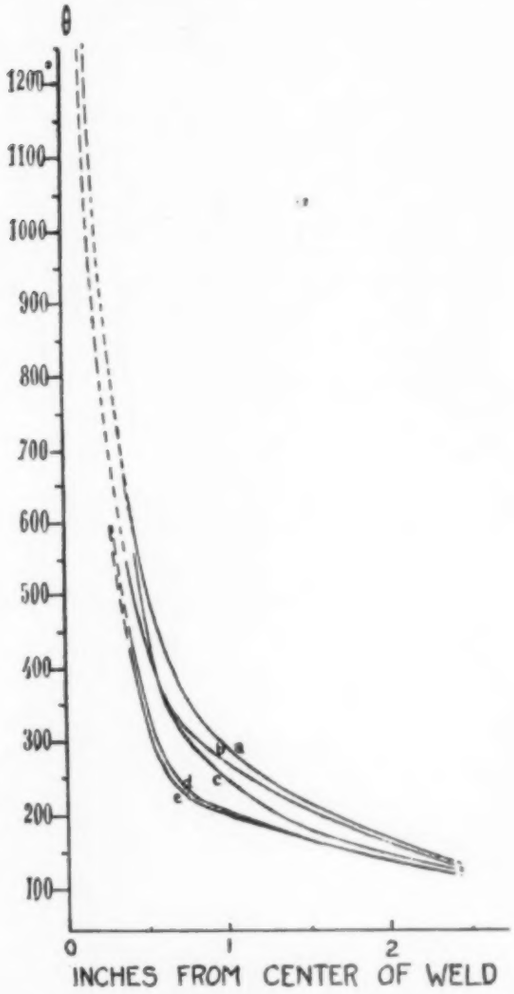

Fig. 2—Maximum Temperature Reached Along a Line Perpendicular to a Covered Electrode Weld in Mild Steel

θ = temperature in ° C.
(*a*) 0.39 inch plates, couples at mid-thickness
(*b*) 0.39 inch plates, couples on surface
(*c*) 0.20 inch plates, couples on surface
(*d*) 0.39 inch square bars, couples at mid-thickness
(*e*) 0.39 inch square bars, couples on surface. Portevin and Séférian[1]

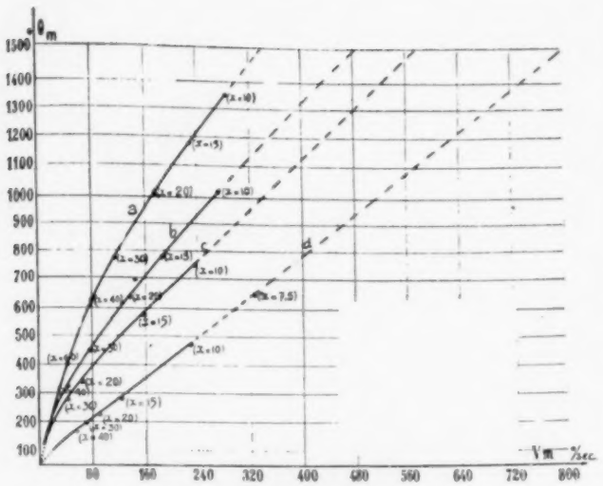

Fig. 3—Maximum Cooling Rates at All Points in the Vicinity of Oxyacetylene and Covered Electrode Welds in Mild Steel

(*a*) oxyacetylene weld, 0.39 inch plates
(*b*) oxyacetylene weld, 0.39 inch sq. bars
(*c*) covered electrode weld, 0.39 inch plates
(*d*) covered electrode weld, 0.39 inch sq. bars
θ_m = maximum temperature attained at any point, ° C.
Vm = maximum cooling velocity, ° C. per second
x = distance in millimeters perpendicular to the weld. Portevin and Séférian[1]

while to review in detail the remainder of the literature on the subject of temperature distribution during welding. Practically none of the investigations can be compared because each used different materials, filler rods, types of specimens and different methods of support, welding, cooling and measurement. The only generalization that emerged was that the temperature of 700° C. was reached at a distance of about 1 inch (very approximately) from the weld for single-layer oxyacetylene welds (V butt) in mild steel plates $^1/_2$ inch thick. Almost all the results are for mild steel and the majority of the results appear in the form of maximum temperature distribution curves, such as Figs. 1 and 2. In most instances, these curves are smooth and approximately of inverse cube or other exponential form. In one or two instances the curves show inflections caused perhaps by irregular advance of the welding heat, or by the A_{3-1} transformation. Time-temperature curves at points near the weld generally show that the rate of temperature rise continues to increase until just before the peak temperature is reached. In the following paragraphs the investigators of temperature distribution are mentioned together with a brief account of the results.

Isotherms for V welds in mild steel, 0.20 inch thick made by the metal arc, atomic hydrogen, arcogen and oxyacetylene processes have been plotted by Bornefeld.[2]

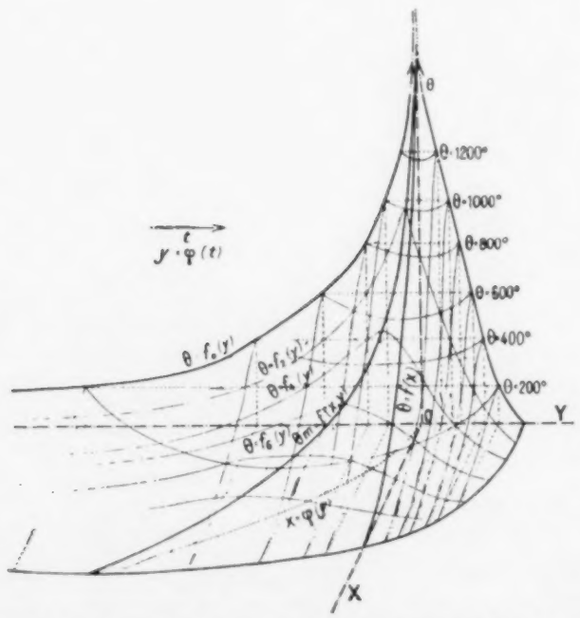

Fig. 4—Temperature Distribution in Oxyacetylene Welded Mild Steel Plates as Torch Is Passing Point 0

OY is the direction of the weld
Arrow shows direction of welding
θ = temperature, ° C. Portevin and Séférian[1]

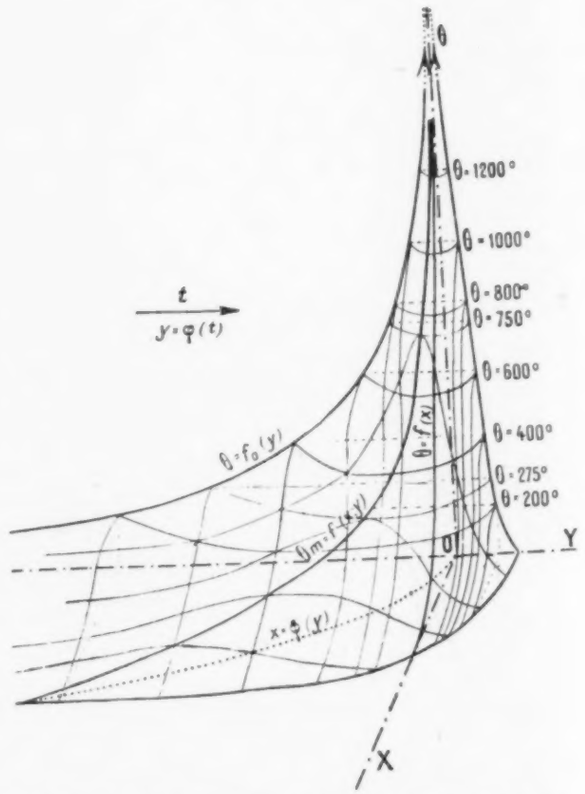

Fig. 5—Temperature Distribution in Covered Electrode Welded Mild Steel Plates as Arc Is Passing Point 0

OY is the direction of the weld
Arrow shows direction of welding
θ = temperature, ° C. Portevin and Séférian[1]

The results are summarized in Table 1. The zone heated to a temperature of 700° C. extended $^1/_4$ inch on either side of the arc weld and $^5/_8$ inch on either side of the gas welds. Temperature gradients were correspondingly mild in the gas welds. Similar results were

Table 1—Extent of Heat-Affected Zones in Welds at Any Instant During Welding. Bornefeld[2]

	Maximum Length (Inches) of Zone in the Direction of the Weld Heated to		Maximum Breadth (Inches) of Zone on Both Sides of Weld Heated to	
Process	300° C.	700° C.	300° C.	700° C.
Oxyacetylene	7.7	2.4	4.0	1.3
Atomic Hydrogen	6.8	2.3	3.2	1.2
Metal Arc	3.4	0.85	1.7	0.5

obtained by Münter[3] for oxyacetylene, arcogen and metal arc welds, the temperature distribution for the arcogen welds being intermediate between those for the others. Shaw[4] made a few temperature measurements in rivets during the reinforcement of a riveted joint by arc and oxyacetylene welding, and also measured the maximum temperature rise at several points along an oxyacetylene or arc-welded joint.

Maximum temperature distribution curves for butt welds (oxyacetylene, atomic hydrogen, metal arc, 1 and passes) in mild steel 0.39 and 0.59 inch thick are given by Lebrun.[5] Similar diagrams have been prepared by:

Reinhard[6] for V butt welds (oxyacetylene and metal arc) in mild steel 0.55 inch thick;

Kinzel[7] for a 90° V. oxyacetylene weld in $^1/_2$ inch steel containing 0.24 C, 0.41 Mn (700° C. at $1^1/_8$ inches from weld);

Rosenthal,[8] who showed that doubling the current (200 to 400 amps.) with the same rate of advance of the electrode reduced the rate of cooling at a given point by about 50%;

Hermann[9] for right- and left-hand oxyacetylene welds in mild steel 0.39 inch thick. Left-hand welding nearly doubled the breadth of the heat-affected zone obtained in right-hand welding;

Kleiner and Bossert[10] for right- and left-hand oxyacetylene welds in mild steel 0.63 inch thick. Results are similar to Hermann's;

British Oxygen Company[11] for right- and left-hand oxyacetylene welding mild steel $^3/_8$ inch thick. The left-hand method, Fig. 8, produces greater heat effect than right-hand. No explanation is provided for the unusual inflections in the curves, although the arrangement of thermocouples may have had some effect;

Politz[12] for cast iron bars brought to fusion by oxyacetylene torch at one end. The heating was linear in time, the cooling exponential;

Bierett and Grüning[13] for mild steel patches 8 inches diameter oxyacetylene welded in plates 80 inches square, 0.39 and 0.59 inch thick.

Other investigators are:

Nikolaev,[14] who reports time-temperature curves at 4 points on a line perpendicular to a V butt weld in mild steel, $^1/_2$ inch thick, covered electrodes;

Gehring,[15] similar curves for arc welds in mild steel $^1/_4$ and $^3/_8$ inch thick;

Schimpke and Horn,[16] maximum temperature distribution curves and isotherms for oxyacetylene and metal arc-welded mild steel plates, 0.39 inch thick;

Leykin,[17] maximum temperature distribution curves for bead $2^3/_4$ inches long deposited on 0.39-inch mild steel plate, 0.30-inch electrodes, 180 amps.;

Burgess,[18] temperature in rail head and fish plate during welding; maximum temperature was 210° F. (100° C.) for metal arc and 430° F. (220° C.) for carbon arc at the same point;

Keogh,[19] temperature in rail head during deposition of pad of metal arc weld metal;

Melcher,[20] tin and wax plugs were placed at several points on one side of an edge-welded plate to show temperature distribution;

Herms,[21] who found that the temperature on the outside of a boiler never exceeded 100° C. during the arc welding of overhead Höhn straps on the inside;

Sandelowsky,[22] temperature distribution in locomotive wheels surfaced by metal arc;

Bartlett,[40] temperature rise near gate valve seats during welding.

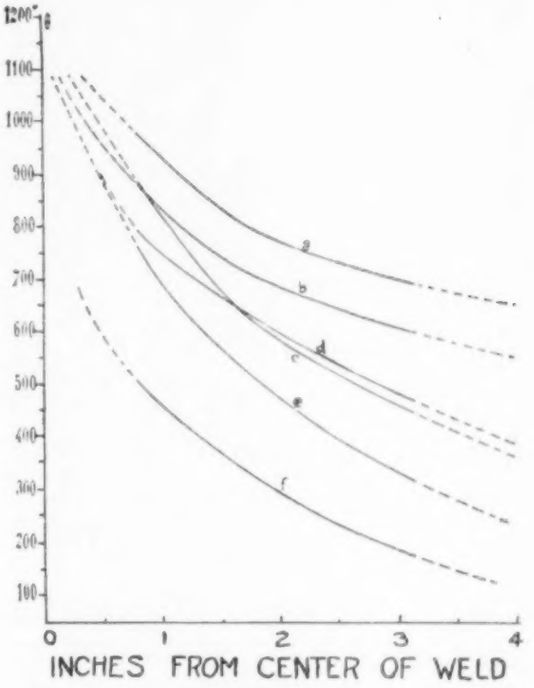

Fig. 6—Maximum Temperature Reached Along a Line Perpendicular to a Forehand Oxyacetylene Weld in Copper

(*a*) 0.39 inch plates, couples at mid-thickness
(*b*) 0.39 inch plates, couples on surface
(*c*) 0.39 inch sq. bars, couples at mid-thickness
(*d*) 0.20 inch plates, couples on surface
(*e*) 0.39 inch sq. bars, couples on surface
(*f*) 0.20 inch diameter round bars, couples on surface. Portevin and Séférian[1]

Plieninger,[23] temperature of flame-cut surfaces of mild steel as a function of oxygen purity. Wanner-pyrometer recorded 1580° C. for $98^1/_2$% oxygen, but only 1420° C. for 80% oxygen purity;

Geruso and Hannant,[24] a few observations on temperature distribution in flame-cut surfaces of mild steel;

Streb and Kemper,[25] temperature distribution during flame hardening; time to reach a dull red heat as a function of gas velocity was determined;

Kleiner,[26] temperature distribution during the flame hardening of brake drums;

Sciaky,[27] temperature distribution during spot welding, Fig. 9.

Heat-Affected Zones

The appearance of the microstructure of a weld has frequently been taken as an indication of temperature distribution. Moe,[28] for example, employed the method in determining the effect of speed of welding on breadth of heat-affected zone. He deposited beads of arc weld metal 16 inches long on horizontal mild steel firebox plates (composition not stated) having a cross section of 0.39 x 3 inches for covered Stabilend 10 (trademark) electrodes (125 amps.) and 0.39 x 4 inches for covered Stabilend 8 (trademark) electrodes (180 amps.), D.C., electrode positive. The specimens rested on a piece of wood 1 inch thick and were quenched in water at 20° C. five seconds after the bead was deposited. The heat-affected zone, Table 2, was considered to be the limit of the gamma zone (880° C.) as revealed by 20% ammonium persulfate etch. The results show that there is little decrease in the size of the

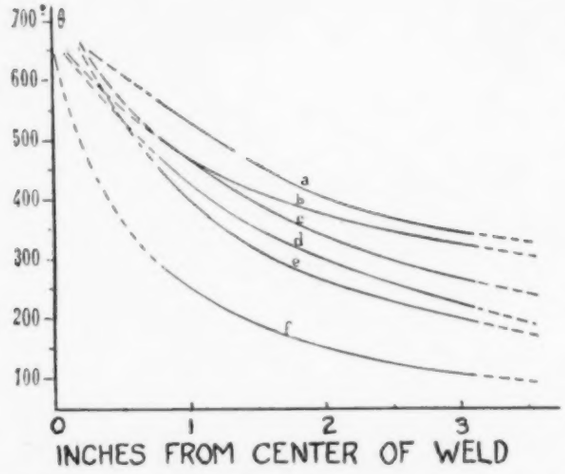

Fig. 7—Maximum Temperature Reached Along a Line Perpendicular to a Forehand Oxyacetylene Weld in Aluminum

(*a*) 0.39 inch plates, couples at mid-thickness
(*b*) 0.39 inch plates, couples on surface
(*c*) 0.39 in sq. bars, couples at mid-thickness
(*d*) 0.20 inch plates, couples on surface
(*e*) 0.39 inch square bars, couples on surface
(*f*) 0.20 inch diameter round bars, couples on surface. Portevin and Séférian[1]

heat-affected zone beyond a critical welding speed; 23 to 26 ft. per hr. for No. 8, and 36 to 40 ft. per hr. for No. 10 electrode.

Less detailed results on the extent of the heat-affected zone are given by Gehring[15] for 70° V. welds in mild steel and low-alloy structural steel 0.24 and 0.39 inch thick welded with bare (140 amps.) and coated (120–125 amps.) electrodes, 0.16 inch diameter. The covered electrodes increased the breadth of the heat-affected zone by 25% above that obtained with bare electrodes. The heat-affected zone was 10% wider in the low-alloy steel than in the mild steel.

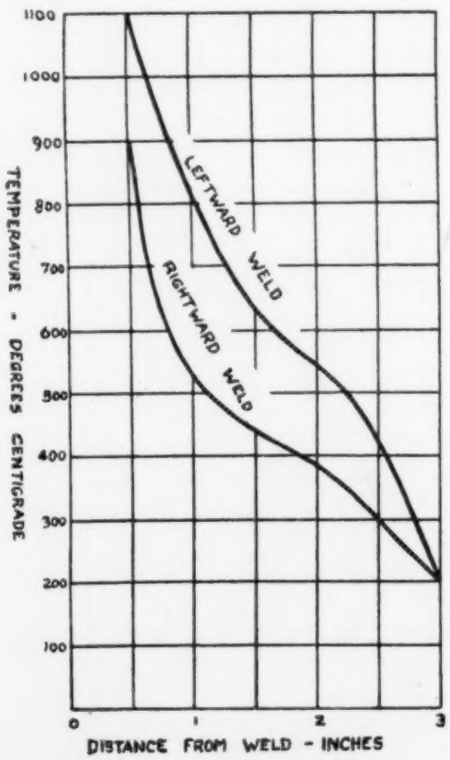

Fig. 8—Maximum Temperatures Attained at Various Distances from Oxyacetylene V Welds in 3/8 Inch Thick Steel Plate, 8 Inches Long. Research Department, British Oxygen Co.[11]

Table 2—Heat-Affected Zone Created by Beads of Arc Weld Metal. Moe[28]

Electrode	Velocity of Advance of Electrode, Ft./Hr.	Cross-Sectional Area of Heat Affected Zone, Sq. In.	Breadth of Heat-Affected Zone, Inches	Cross-Sectional Area of Bead, Sq. In.	Width of Bead, Inch	Penetration Inch
Stabilend 8	16.4	0.42	1.18	0.059	0.98	0.04
	23.0	0.271	0.83	0.044	0.79	0.07
	27.5	0.178	0.71	0.031	0.55	0.08
	30.2	0.124	0.59	0.023	0.47	0.08
	38.7	0.065	0.51	0.022	0.43	0.08
	66	0.047	0.39	0.012	0.32	0.06
	100	0.028	0.32	0.008	0.24	0.06
Stabilend 10	8.7	0.31	1.18	0.078	0.98	0.04
	14.8	0.186	0.83	0.053	0.67	0.04
	26.0	0.068	0.47	0.031	0.39	0.04
	35.2	0.053	0.43	0.020	0.35	0.04
	39.4	0.044	0.39	0.012	0.32	0.03
	45	0.031	0.32	0.011	0.28	0.02

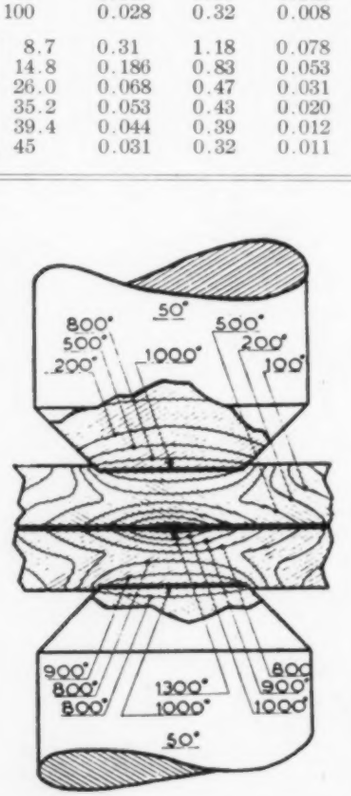

Fig. 9—Temperatures Developed During Spot Welding. The Temperatures Are Largely Deduced from Microstructural Observations, etc., and Have Not Been Verified Experimentally. Sciaky.[27]

Results are also given by Rapatz,[29] for welds in mild steel, 0.39 inch thick. The overheated zone, as judged by microstructure, was 0.83 inch wide in oxyacetylene welds, 0.08 inch wide in covered electrode welds, and 0.04 inch wide in bare electrode welds. According to J. C. Hodge (private communication, July 1937), the depth of the heat-affected zone in covered electrode welds in steel is ordinarily 3/16 inch from the line of fusion between weld metal and plate, covering all transitions from the fusion line to the zone of spheroidized pearlite. Kinzel[7] showed that there is a close connection between the temperature distribution curve in oxyacetylene welded mild steel and the mechanical properties and microstructure. The well known changes in grain size

and structure in the heat-affected zone could be predicted from the temperature distribution results.

The application of temperature distribution curves and cooling velocity curves derived therefrom to the prediction of martensite formation, assuming the critical cooling rates of overheated base metal and weld metal are known, has been discussed by Portevin[1] and Rosenthal,[8] among others. Nothing of a quantitative nature has been developed as yet.

Theoretical

The calculation of temperature distributions during welding has reached the approximate stage in which such factors as radiation and convection,[30] heats of solidification and transformation, and changes in physical constants with temperature are allowed for in an empirical way. For quasi-instantaneous welding processes, such as resistance welding, satisfactory equations have been developed for simple shapes and conditions. Good approximate methods of calculating temperature distributions in straight butt welds in thin plates welded at constant velocity by arc or gas processes are also available.

Instantaneous Source of Welding Heat

Simple methods for computing temperature distributions in welding are given by Ingersoll and Zobel.[31] If the heat Q supplied to weld two bars instantaneously is known, the maximum temperature T reached at any point x beyond the weld during cooling may be computed from the formula

$$T = \frac{Q}{67.6x}$$

where T is rise in temperature in degrees Centigrade, Q is in calories per sq. in. x is in inches. If the quantity of heat is not known but the maximum temperature of the weld is known, the formula for temperature distribution involves the time factor and the Probability Integral (Gauss Function).

The latter method is required also in the formulas developed by Okamoto[32] for resistance butt welding. Starting with the differential equation of heat conduction in one dimension, and assuming that the power at the contact surface decays from its initial value according to an exponential in time as welding progresses, Okamoto arrived at a distribution, Fig. 10, involving the Gauss function. The assumptions are that the bars are infinitely long, that the emissivity is constant, and that

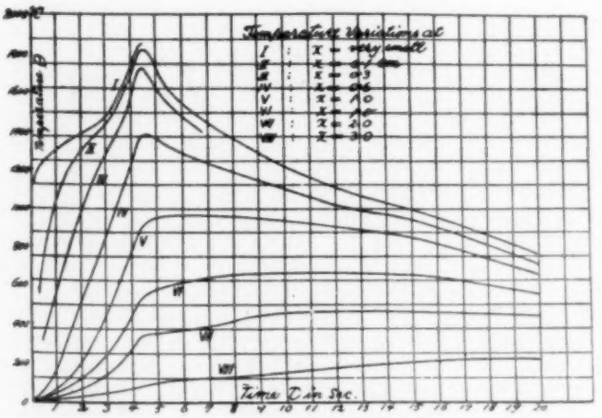

Fig. 10—Calculated Temperature Distribution During and After Resistance Butt Welding Two Mild Steel Bars

Time of welding—4.4 seconds
x = distance from welded surface. Okamoto[32]

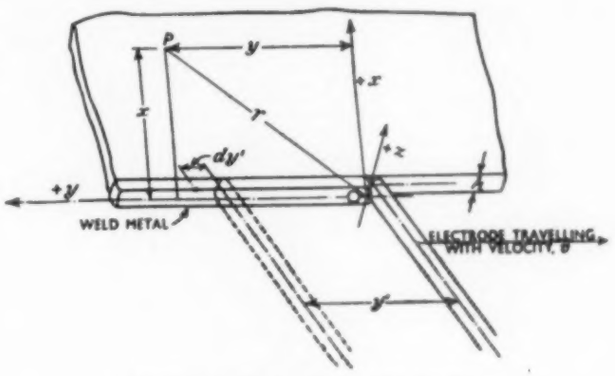

Fig. 11—Notation Used by Boulton and Lance Martin[33]

heat transfer occurs axially and by radiation from the surface, the loss by radiation varying linearly with temperature (Newton's law). Equations for percussion welding (time of heating is negligibly small) and for two bars having different thermal constants are also developed.

A frequency distribution is assumed by Palmblad[33] to represent the temperature, Fig. 11, in a thin spot-welded disk. The equation is

$$T = T_o e^{-hr^2}$$

where T_o = temperature at spot weld (center of disk, $r = o$)
r = distance of any point from center
T = temperature at any point distant r from center
h = a constant, assumed to be 0.05 cm^{-2} for mild steel.

Other approximate theoretical temperature distributions for welded joints have been proposed by Helfrich,[34] Kinkead[35] and Varriot,[36] the last named considering that, with a point source of heat, the temperature in each elementary annular ring of equal radial thickness varies inversely as the volume of the ring. An empirical logarithmic relationship has been suggested by Leykin[17] and Nikolaev:[14]

$$T = T_o e^{-0.5P}$$

where P = distance from weld, mm.
T_o = temperature at weld, ° C.
T = temperature at a point distant P from weld
0.5 = experimental constant for arc welds in mild steel, found by Leykin.

Nikolaev found that the constant varied from 0.604 to 0.768 for three different experiments on arc welds in mild steel, the average being 0.653 cm^{-1}.

In addition to the theoretical equations for temperature distribution in welded joints, there is a considerable amount of information on temperature distributions not connected with welding. The general literature on the subject has been compiled by the Welding Research Committee in the form of an annotated bibliography for the convenience of research workers.

Moving Source of Welding Heat

Formulas for temperature distribution as a point source of heat (arc or torch) moves along an infinite thin plane in a straight line at constant velocity have been derived by Pinczon,[37] whose starting point is the differential equation for heat conduction in two dimensions. He makes the ordinary assumptions of constancy of

thermal coefficients and arbitrary radiation and convection loss, and arrives at formulas involving the Gauss function. Pinczon applies his formulas to numerical examples, the calculated temperature distributions bearing a close resemblance to the experimental curves determined by Portevin and Séférian. Portevin and Séférian, who adopt Pinczon's ideas, showed that Pinczon's formula may be approximately written

$$T = T_o\, e^{-\frac{cd}{x}(x - v\,t)v}$$

for very high velocities of welding, v > 0.5 cm./sec. (1 ft. per min.), where T = temperature, ° C. of point at perpendicular distance x from the weld;

v = velocity of advance of source of welding heat, cm./sec.;
t = time at any instant from start of weld;
T_o = temperature at source of welding heat;
c = diffusivity;
d = density.

A similar formula has been evolved by Okada[38] on the basis that a weld is made up of an infinitely large number of small instantaneous heat sources established in succession along a straight butt weld. The sources were considered for purposes of calculation as being 1 calorie per mm. (the temperatures involved are correspondingly low), and placed 1 mm. apart, moving at velocities of 0.5, 1 and 2 mm. per sec. (1.18, 2.36 and 4.72 inches per minute). The temperature was assumed to be independent of plate thickness and the differential equations for heat conduction in two or three dimensions were used as starting point. The usual assumptions concerning constancy of thermal coefficients and empirical factors for external heat loss were made.

Being a step-by-step process, the computations require time and care, but Okada nevertheless prepared over a hundred charts involving thousands of individual calculated points. The isotherms and temperature distributions calculated are similar in form to those determined experimentally by Bornefeld[2] and others, but the method of computation appears to be more laborious than, and not superior to, the methods of Pinczon and Boulton and Lance Martin.[39]

The formula developed by Boulton and Lance Martin is

$$T = \frac{Q}{\pi k}\, e^{By}\, K_o\,(BGr)$$

where T is the temperature rise at any point P, Fig. 11, at the instant the electrode is passing the origin, O;

Q is the "strength" of the permanent traveling line (parallel to OZ) heat source (electrode);
k is diffusivity of the plate (0.0186 sq. in. per sec.)
$B = v/2k$
$G = \sqrt{1 + (4kL/v^2)}$
$L = 2E/cPh$ (assumed 0.000733 per sec., British units);
E is the average emissivity of the faces of the plate;
P is the density of the plate;
c is the specific heat of the plate;
$r = \sqrt{x^2 + y^2}$
$hPcQ$ is the quantity of heat entering the plate in unit time;
$K_o(BGr)$ is a modified Bessel function of the second kind of zero order.

In the derivation heat loss is assumed to be proportional to temperature above surroundings, the radiation and convection occurring from the faces but not from the edge of the plate, which is assumed to extend indefinitely in the positive direction of x and in both directions along the y-axis. The thermal coefficients of the plate are assumed to be constant. As Boulton and Lance Martin show, the maximum temperature rise at any point is readily obtained from the formula.

The calculation of temperature rise at any point using Boulton and Lance Martin's formula is not difficult, providing a table of $e^{m}\, K_o\,(M)$ is available. Trouble arises, however, in selecting values for Q and the thermal coefficients. The value 0.0186 for k was determined by the investigators during welding experiments in which thermocouples were placed at different points on the edge-welded plates (no details). The same procedure was adopted to determine Q, which was found to be 36.4 sq. in. per ° C. per sec. for a mild steel plate 9 ft. long, 6 x $^3/_8$ inch cross section welded on one long edge (No. 8 gage asbestos covered electrodes, 105 amps. speed of electrode 3.08 inches per minute, manual welding) and 54.4 for a similar plate welded on both long edges simultaneously (same electrodes but 115 amps., 6.22 inches per minute). The constants were so chosen, of course, that the calculated temperature distribution exactly agreed with experimental results. If it were found by further experiment that the formula could be adjusted to allow for different radiation and convection conditions, and different welding conditions, as well as possibly for minor factors, such as heats of solidification and transformation, it would be useful to summarize the results in the form of charts or nomographs on a broader and more reliable basis than the temperature distribution charts included in the present review.

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Fusion Welding Wrought Iron

A Review of the Literature to January 1, 1937

By W. SPRARAGEN* and G. E. CLAUSSEN†

This Report Is Prepared Under the Auspices of the Literature Division of The Engineering Foundation—Welding Research Committee

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INTRODUCTION

Before the common methods of fusion welding had been developed, wrought iron, among all ferrous materials, possessed the best weldability by reason of its characteristic properties and structure. Wrought iron still enjoys its high reputation for forge weldability but the processes of fusion welding by arc, gas and electric resistance are, fortunately, as applicable to structural steels as to wrought iron. The structure and properties of wrought iron differ sufficiently from those of mild steel to create definite, though minor, differences in their fusion welding characteristics. It is the purpose of the present review to reveal these differences from the standpoints of methods of fusion welding, metallurgical characteristics and physical properties.

SUMMARY

Oxyacetylene Welding

The oxyacetylene welding of wrought iron consists in maintaining a puddle of molten metal at the point of application of the filler rod. The end of the rod is immersed in the puddle until sufficient metal has been deposited. Then the edges of the molten puddle are fused into the surrounding colder metal. The weld progresses by repetition of this procedure. The molten metal should be disturbed as little as possible to permit the slag to rise from the molten metal.

The greasy appearance of the edges to be welded noticeable in welding corresponds to a temperature of 2100 to 2200° F. (1150 to 1210° C.), the correct temperature for welding being 2700 to 2750° F. (1480 to 1510° C.), a little higher than for mild steel.

The filler rod may be low-carbon steel or a high test rod. Neutral flame is considered best.

Metal Arc Welding

Recommended procedure involves the use of slightly slower welding speeds than for mild steel of the same thickness. In this way the metal is kept fluid longer, and gases and slag are eliminated. It may be necessary to use a slightly lower current than for the same thickness of mild steel, especially in thin sections where burning through is possible. Excessive penetration into the face of the metal should be avoided. High-grade, unalloyed, low-carbon electrodes should be used, bare or coated.

Carbon Arc Welding

The carbon arc welding of wrought iron is the same as of mild steel.

Resistance Welding

There have been no problems in the flash welding of wrought iron of sufficient importance to distinguish the process from the flash welding of steel. The temperature must be around 1510° C. The problem in resistance butt welding is to avoid re-aligning the slag fibers in the upset zone in such a way that the fibers are transverse to load. This may be accomplished by properly balancing pressure and power.

Metallurgy

In both arc and gas welds the weld metal contained practically no slag and there was no balling-up or agglomeration of the slag at the junction between base metal and weld. The absence of the A_1 point in wrought iron is an advantage in reducing internal stress and in eliminating air-hardening capacity. The bond in bronze-welded wrought iron is due mainly to surface alloying, and partly to intergranular penetration.

Mechanical Properties of Arc-Welded Wrought Iron

Heat effects of deposited beads on wrought iron bars in a direction parallel to stress may reduce the tensile strength of the base metal 5 to 10% and in a direction perpendicular to stress this reduction may be of the order of 10 to 15%. Tests demonstrate that worn or corroded

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wrought iron structures may be successfully reinforced by welded plates. The values given by the A. W. S. Building and Bridge Codes for steel may be used. End fillets added to side fillets increase the maximum load but do not increase the yield strength. The amount of weld metal in contact with wrought iron has a pronounced effect on the maximum load but not on the yield strength. Inelastic action somewhat modifies the stress distribution until the maximum load is reached, which depends on the area of metallic contact of weld metal with wrought iron.

It is recommended that the leg of the fillet on the surface of wrought iron parts be about twice that of the other leg. Side fillet welds in wrought iron have a shear strength of 40,000 psi; butt welds have a tensile strength of 43,000 psi.

The X-ray requirements of the A. S. M. E. Boiler Code for Class I welds are met by welded wrought iron.

Mechanical Properties of Oxyacetylene Welded Wrought Iron

Gas welds have the same tensile and shear strength as arc welds. Design values in the Building and Bridge Codes are safe. Resistance butt welded joints in wrought iron have 90 to 100% of the tensile strength of unwelded base metal, 0 to 150% of the elongation of base metal and 86% of the rotating cantilever fatigue strength (Old data).

Applications

Old Bridges.—The specifications for Highway and Railway Bridges of the American Welding Society (1936) state that in the application of wrought iron to bridge repair, the iron must be so placed that the direction of maximum stress must coincide with the direction of rolling.

Miscellaneous Applications.—The major applications of welded wrought iron are probably wrought iron pipe and fittings. Welded boiler and tank parts are also mentioned in the literature. Almost all wrought iron chain in this country is resistance butt welded.

Welding Wrought Iron to Other Metals

There appears to be no difficulty in welding wrought iron to mild steel by arc or gas processes. Wrought iron has been metal arc welded to high speed steel. It has also been successfully welded to cast iron by means of a covered mild steel electrode.

Flame Cutting

It is generally considered that there is no difficulty whatever in flame cutting wrought iron using the same procedure as for mild steel.

Fusion Welding Wrought Iron

Methods of Fusion Welding Wrought Iron

Oxyacetylene Welding

The oxyacetylene welding of wrought iron, according to Aston,[1] consists in maintaining a puddle of molten metal at the point of application of the filler rod. The end of the rod, which should be low-carbon iron, is immersed in the puddle until sufficient metal has been deposited. Then the edges of the molten puddle are fused into the surrounding colder metal. The weld progresses by repetition of this procedure. The molten metal should be disturbed as little as possible to permit the slag to rise from the molten metal. Landes,[2] who summarizes the recommendations of the American Bureau of Shipping and Lloyd's Register of Shipping, is in agreement with Aston. The welder should be aware that the greasy appearance of the scarves at a relatively low temperature is caused by melting of the slag in the base metal. Fusion of the wrought iron requires a considerably higher temperature (Appendix).

The recommended procedure is given in Table 1.

Table 1—Manual Oxyacetylene Welding Procedure to Produce Butt Welds in Wrought Iron Conforming to the X-Ray Requirements of A. S. M. E. Boiler Code for Class 1 Welds. Landes

Thickness of Plate, inch	1/8	1/4	3/8	1/2	5/8	3/4	7/8	1
Type of Joint				Single V				
Joint Angle, Degrees	60	80	80	80	80	80	80	80
Spacing (Inch) Between Edges (starting end)	1/32	1/16	1/16	1/16	1/16	1/16	1/16	1/16
Spacing 18 Inches from Starting End	1/8	3/16	1/4	1/4	1/4	1/4	1/4	1/4
Diameter of Filler Rod, Inch, Face Side	1/8	3/16	1/4	1/4	1/4	1/4	1/4	1/4
Diameter of Filler Rod, Inch, Root Side*	1/8	3/16	3/16	3/16	3/16	1/4	1/4	1/4
Number of Layers, Face	1	1	1	2	3	3	4	5
Number of Layers, Root	1	1	1	1	1	1	1	1

* When a double weld is required a root bead is used.

The term "layer" in Table 1 refers to a step-by-step method of depositing beads illustrated in Fig. 1 (private communication from B. D. Landes, August 1937). The step-by-step method is preferable to others because it conserves heat, produces sounder weld metal and minimizes cracking. Fewer steps than those listed in Table 1 may be used, but it is difficult to keep the larger volume of molten metal in the weld zone. Figures 2, 3 and 4 illustrate the welding methods advocated by B. D. Landes.

According to *Boiler Maker*,[3] the greasy appearance corresponds to a temperature of 2100 to 2200° F. (1150 to 1510° C.), the correct temperature for welding being 2700 to 2750° F. (1480 to 1510° C.), a little higher than for mild steel. Wrought iron welding rod is no longer used. Landes[2] emphasizes that the filler rod should have a yield point of 27,000 to 30,000 psi (all-weld-metal), and that carbon, consequently, should be low and alloying elements absent. Raymond,[4] however, states that a Norway iron or high-strength rod may be used and adds that a neutral flame is best. Welding[3] also advocated a "high-test" rod for wrought iron pipe. This rod usually contains 0.15–0.20 C, 0.30–0.40 Si, 0.80–1.0 Mn, and deposits steel containing about 0.18 C, 0.20 Si, 0.45 Mn. The A. R. E. A. Specifications[7,4] for welded wrought iron chimneys require that a neutral flame always be used for gas welding.

Wrought iron pipe[6] is particularly suited for oxyacetylene welding for two reasons. First, wrought iron does not tend to spark during welding. Since sparking is a source of oxide in the deposited metal, welds made in wrought iron tend, surprisingly enough, to be less contaminated with oxides than welds in steel. In the second

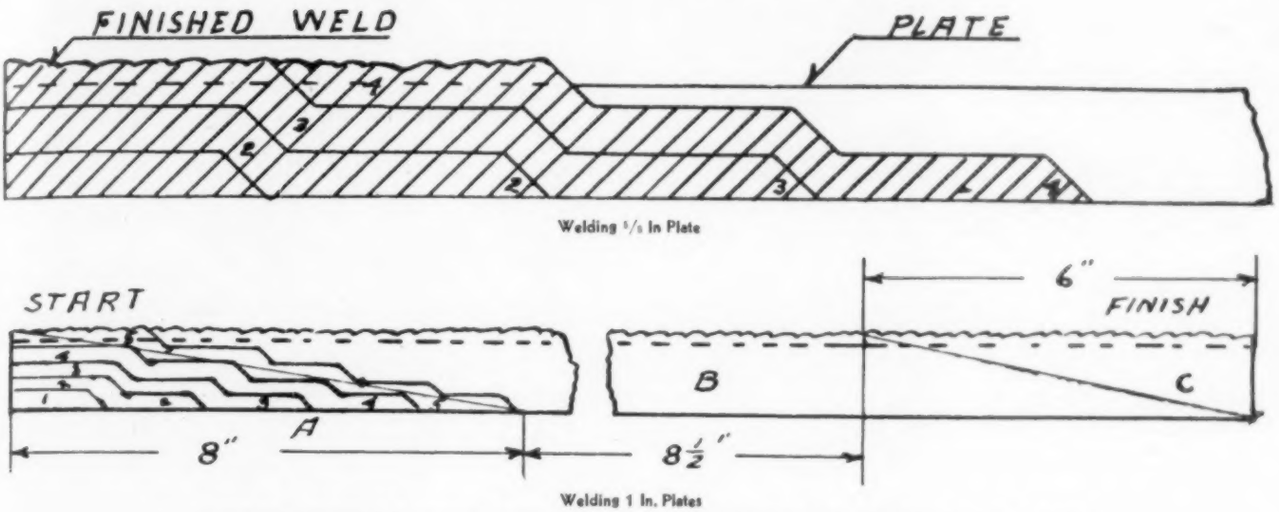

Fig. 1—Method of Depositing Layers by the Oxyacetylene Process in 5/8-Inch and 1-Inch Wrought Iron Plates (B. D. Landes)

place, molten wrought iron is more viscous than molten steel. The molten wrought iron is therefore easier to manipulate than molten mild steel, and good root penetration is easily secured. A French welder[7] found that flux (no details) greatly facilitated the gas welding of wrought iron.

A number of welders[8] consider wrought iron as easy to weld as mild steel. A British Standard (No. 640) and a pipe welding manual[9] make no distinction between the oxyacetylene welding of wrought iron and mild steel. The U. S. Navy Bureau of Construction and Repair (private communication, August 1937) adopt the same welding technique (process not specified) for wrought iron pipe as for medium steel. On the other hand, Kayser[10] encountered difficulty in welding old wrought iron with a copper-coated mild steel rod intended to produce weld metal containing 0.15 Cu. He found it necessary to have an oxygen-acetylene ratio of 1.2 and, even so, the welding was more difficult than for mild steel.

Metal Arc Welding

The procedure for metal arc welding wrought iron recommended by Landes,[2] Table 2, on the basis of tests made by the American Bureau of Shipping and Lloyd's Register of Shipping, involves the use of slightly slower welding speeds than for mild steel of the same thickness, Table 3. In this way the metal is kept fluid longer, and gases and slag are eliminated. It may be necessary to use a slightly lower current than for the same thickness of mild steel, especially in thin sections where burning through is possible. Excessive penetration into the face of the metal should be avoided. High-grade, unalloyed, low-carbon electrodes should be used, bare or coated. For relatively unimportant welds higher currents and speeds, and larger electrodes may be used than shown in Tables 2 and 3.

These recommendations differ materially from those made by Aston[1] in 1932. Aston stated that 10% greater

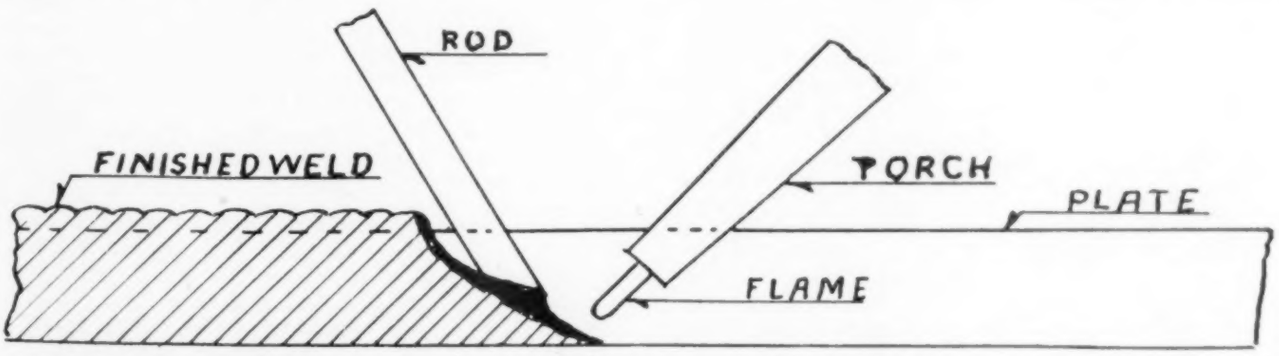

Fig. 2—Backhand Welding Technique for Oxyacetylene Welding Wrought Iron, Showing the Relative Position of Weld, Rod and Torch (B. D. Landes)

Table 2—Manual Metal Arc Welding Procedure to Produce Butt Welds in Wrought Iron Conforming to the X-Ray Requirements of A. S. M. E. Boiler Code for Class I Welds. Landes[2]

Thickness of Plate, Inch	1/4	3/8	1/2	5/8	3/4	7/8	1
Type of Joint	Single V	Single V	Single V	Single U	Single U	Double U	Double U
Joint Angle, Degrees	60	60	60	18	18	18	18
Radius, Inch	None	None	None	1/4	1/4	1/4	1/4
Tongue, Inch	0 to 1/16	0 to 1/16	0 to 1/16	3/32	3/32	3/32	3/32
Spacing Between Edges, Inch	5/32	5/32	5/32	None	None	None	None
Number of Passes	3	4	5	6	8	9	10
Diameter of Electrode, Inch	3/16	3/16	3/16	3/16	3/16	3/16	3/16
Amperes	170	170	180	180	180	180	180

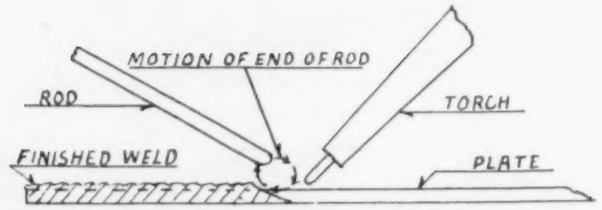

Fig. 3—Motion of Rod for Oxyacetylene Welding Light Wrought Iron Plates (B. D. Landes)

current densities should be used for wrought iron than for mild steel in order to secure penetration. In his recent instructions Aston[1] adopts Landes' ideas that slightly lower speeds should be used than for mild steel and slightly lower currents in light sections where there is

Table 3—Recommended Rate of Travel of Electrode (Inches per Minute) for Manual Metal Arc Welding. Landes[2]

Thickness of Plate, Inch	1/4	3/8	1/2	5/8	3/4	7/8	1
First Layer, First Side	8	8	7	7	7	6	6
Intermediate Layers, First Side	5	5	5	5	5	5	5
Last Layer, First Side	5	5	4	4	4	4	4
First Layer, Second Side	5*	5*	4*	4*	4*	6	6
Intermediate Layers, Second Side						5	5
Last Layer, Second Side						4	4

* Not used unless a double weld is specified.

a danger of burning through. Although Aston gives no reason for his reversal of belief, it would seem that excessive current density might adversely affect the slag inclusions (agitation) whereas slower speeds at lower currents achieve the desired result of high welding temperature yet permit rise of slag to the surface of the pool.

Bare electrodes for welding wrought iron should contain 0.13–0.18 C, 0.40–0.60 Mn, not over 0.06 Si, not over 0.04 P, not over 0.04 S, according to McKibben.[12] For 1/4 to 3/8 inch fillets on 1/4 to 3/8 inch stock the electrode should be 5/32 inch diameter, 160–180 amps., electrode negative. It is now realized that any good quality low-carbon electrode is satisfactory. Lacy[13] obtained good results with an asbestos wound electrode. A number of writers[14] consider that there is no difference between the arc welding of mild steel and wrought iron.

Carbon Arc Welding

According to Unland,[70] the carbon arc welding of wrought iron is the same as of mild steel. Howard[15] (1890) used the carbon arc to weld wrought iron cylinders, 0.079 inch thick, 9 inches diameter, the welds having 92% of the tensile strength of base metal.

Resistance Welding

Wrought iron has been placed at the top of a well known list[71] of metals in the order of weldability by the resistance process. There have been no problems in the flash welding of wrought iron of sufficient importance to distinguish the process from the flash welding of steel.

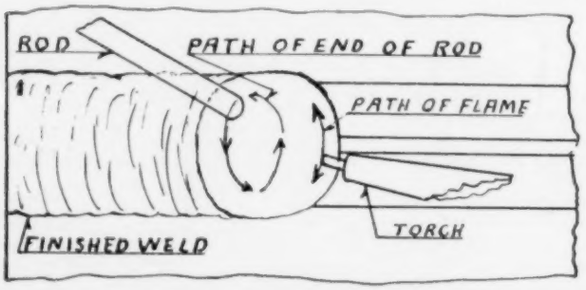

Fig. 4—Motion of Torch and Rod for Oxyacetylene Welding Heavy Wrought Iron Plates (B. D. Landes)

H. A. Woofter (private communication, August 1937), to whom the reviewers are indebted for the information in this paragraph, states that the flash welding of wrought iron is simple and is widely used in the production of pipe. The temperature must be high enough (2750 to 2800° F. = 1510 to 1540° C.) to fuse the iron, in which case the slag is all thrown out at the time of welding leaving clean metal in the weld with no trace of slag stratification. B. D. Landis (private communication, September 1937) points out that upset in pipe welding should be as small as possible to avoid transverse structure of slag.

From what little information there is on the resistance butt welding of wrought iron, it appears that the method, which, according to H. A. Woofter, is nearly obsolete in this country except for chain, is not suitable as applied by some welders. Nápravník and Popov[16] found that the fracture of resistance butt-welded wrought iron appeared perfect, but macro- and micro-examination showed that slag stratifies at the weld because the surfaces to be welded are perpendicular to the slag fibers. Although the slag protects the metal from oxidation, the fibers are upset during welding and reduce the area of metallic weld in the direction of load. Paterson[17] also appears to have noted the difficulty with slag stratification. Blomberg[19] found that slag-rich wrought iron cannot be expected to produce high-strength resistance butt welds. Aston[1] found no difficulty if somewhat higher current densities were used than for mild steel. According to H. A. Woofter, the correct pressure for resistance butt welding wrought iron to avoid porosity is 5000 to 10,000 psi, depending upon the shape of the parts. The ends of the chain stock are cleaned free from slag and rust before welding. As early as 1890 Bramwell[18] was successful in obtaining resistance butt welds in wrought iron having a tensile strength 90% of base metal. It is not clear whether slag stratification is a difficulty in spot welding in which upset is small and in which the surfaces to be welded may be parallel to the slag fibers.

Metallurgy

It is generally believed that in gas and arc welding wrought iron there is no metallurgical principle involved that is not present with pure iron. This is Aston's opinion[1,11] based on micro-examination of oxyacetylene, metal arc and carbon arc welds. All welds showed excellent penetration. The weld metal contained practically no slag and there was no balling-up or agglomeration of the slag at the junction between base metal and weld. Aston believes that the slag acts as a natural flux and is therefore beneficial. The British Engine, Boiler & Electrical Insurance Co.[20] observed no balling-up of slag in welds made with bare electrodes. The microstructure of welds made with blue asbestos covered electrodes, according to W. D. Chapman,[21] showed globular slag at the junction between weld and wrought iron. Chapman considered that the globular slag was less dangerous than the fibrous slag in base metal.

Since the slag has a lower melting point than iron, a French welder[22] believed that the oxyacetylene flame or the arc melts out the slag, which collects in globules and leaves voids in base metal at some distance from the weld where slag has melted but iron has not. This belief appears to have no basis. The slag fibers are small and their surface tension prevents voids. Rawdon and Knight[23] found that the melting ranges of slag from hand puddled and Aston process wrought irons were: Aston slag commenced to melt at 1280° C., completely molten at 1350° C. Hand-puddled slag commenced to melt at 1250° C., completely molten at 1350° C. The

samples of slag were contained in iron crucibles heated in an Arsem vacuum furnace at 1 mm. mercury or less. Fletcher[24] estimated (no details) that the slag content of wrought iron causes a loss of 1% of welding surface as estimated from micrographs, considering the grains as cubes.

Fletcher also pointed out that the absence of the A_1 point in wrought iron is an advantage in reducing internal stress and in eliminating air-hardening capacity. According to Wallis,[25] annealing after fusion welding is not necessary for wrought iron unless it is subject to vibratory stress in service. Cracks should not be welded. Atkins[26] states that segregations of high-phosphorous metal in high-phosphorous wrought iron melt before the low-phosphorous metal, flow into the joint and embrittle it.

The carbon pick-up in carbon-arc welding wrought iron (0.01% C) with a pointed, hard carbon electrode (no filler rod) has been determined by Doan and Ekholm.[27] With the electrode vertical, arc length 3/8 inch, rate of travel of arc 4 inches a minute, the carbon content increased to 0.015% C in one test and was unchanged in two other tests.

A thorough study by Jackman[28] of the microstructural aspects of bronze-welded wrought iron showed that the bond is due mainly to surface alloying, and partly to intergranular penetration. Beads of three bronzes, Table 4, were deposited on wrought iron plates by an expert welder using 3/16 inch rod, borax flux and an oxy-acetylene torch. There were no discontinuities in the junction of bronze with iron, which showed that alloying (tinning) had occurred. Intergranular penetration was also observed. The bronze occasionally surrounded islands of slag, probably on account of capillary action. There was no difference between the results with the three bronzes. Heat treatment for 6 hrs. at 820 or 850° C. did not widen the diffusion zone. After 24 hrs. at 900° C., which was close to the melting point of the bronzes (no details) intergranular penetration had increased and etching revealed a fine, dotted structure in the zone of penetration. The zone was about 0.006 inch deep and, so far as Jackman's micrographs show, the intergranular penetration was no deeper after heat treatment than before, contrary to his expressed conclusion. The intergranular penetration of bronze III was less than that of the other two possibly because bronze III has a higher melting point than the others.

Table 4—Bronzes Tested by Jackman[28]

	Cu	Zn	Sn	Fe	Ni
I	60.0	39.25	0.75	...	...
II	59.0	40.3–39.0	0.5–1.5	0.2–0.5	...
III	57.0	39.3	...	1.5	2.2

In order to magnify the effect of molten bronze and its constituents on wrought iron, Jackman etched ground specimens of wrought iron in 10% HCl, immersed them in molten borax flux, and submerged them in each liquid bronze at 975° C. for 20 hrs. There was only a slight loss of zinc during the experiment. The same microstructural characteristics were observed in these specimens as in the bead deposits but on a larger scale. Intergranular penetration and alloying occurred to greater depths than in the brazed specimens and there was indication of back-diffusion of iron into the bronze. Similar etched and fluxed specimens were immersed in zinc (6 hrs., 550° C.), copper (20 hrs., 1150° C.), and tin (7 hrs., 425° C.). Zinc and tin formed compounds with the iron, both of which penetrated along the grain boundaries. Intergranular penetration and diffusion were observed in all cases.

The same bronze-welding technique appears to apply to wrought iron as for steel.[73]

Mechanical Properties

As in previous reviews of literature on other ferrous metals, the mechanical properties of fusion-welded wrought iron are summarized in tabular form in this section, chronologically and according to welding process. It will be seen that the strength of fillet welds is better known than the properties of butt welds and that little beyond tensile and deflection tests has been attempted. The effect of welding on impact value, for example, remains a subject for speculation.

The unit stresses adopted in the A. R. E. A. Specifications[74] for welded wrought iron chimneys are:

Shear on net section through throat of weld	11,300 psi
Tension on net section through throat of weld	13,000 psi
Compression on net section through throat of weld	15,000 psi
Tension in unwelded wrought iron plates (net section)	12,000 psi

Fillet welds placed transverse to the direction of stress are considered as under shear. The stress in the weld is calculated on net section through the throat, excluding reinforcement. The working stresses for standard fillet welds in wrought iron are given in Table 5.

Table 5—Working Stresses of Standard Fillet Welds in Shear, Adopted by A. R. E. A. Specifications for Welded Wrought Iron Chimneys[74]

1/4 inch fillet	2000 lb. per linear inch
5/8 inch fillet	3000 lb. per linear inch
1/2 inch fillet	4000 lb. per linear inch
5/8 inch fillet	5000 lb. per linear inch

The corrosion problem in fusion welded wrought iron has been discussed by Liedloff[29] and Gross.[30] Liedloff observed caustic embrittlement in an oxyacetylene welded wrought iron tank for hot NaOH solution, 40° Be, which he attributed to incorrect welding sequence and high shrinkage stresses. Gross found that arc-welded wrought iron pipe for technical H_2SO_4 developed leaks after only six months but that flanged joints remained tight for several years. As Kemper remarked, the welds doubtless were improperly made.

Arc-Welded Wrought Iron

Investigator

Jennings[31]—1937

Shielded arc welds in wrought iron have an elongation of 26 to 36% in 2 inches (no details).

Sahling[32]—1936

Beads deposited on surface (no welding details).

(*a*) Bead in direction of tensile stress on one side of a flat wrought iron bar cut from an old bridge, cross section of bar = 0.62 sq. in.
Cross section of bead = 0.08 sq. in.
Ratio of cross sections = 12.5% (in practice the highest ratio is usually not more than 6%).
Tensile strength was reduced in one case from 45,000 to 40,700 psi, and in another case from 53,000 to 43,500 psi. The reduction may have been caused partly by eccentric loading.

(*b*) Beads in direction of tensile stress on both sides of a flat wrought iron bar (tensile strength 54,500 psi).

Beads in middle third of bar	45,700 psi
Ditto, beads ground flush	44,600 psi
Beads over whole length of bar	48,000 psi
Ditto, beads ground flush	40,100 psi

The base metal for the last test had a tensile strength of only 51,000 psi.

(*c*) Beads transverse to direction of tensile stress on one side of a flat wrought iron bar (tensile strength 53,000 psi).

Beads on one side	49,800 psi
Ditto, ground flush	45,800 psi

(*d*) Same as (*c*) but beads on both sides

Tensile strength of original bar =	55,500 psi
Beads on both sides	51,000 psi
Ditto; ground flush	43,600 psi

Fracture occurred in the beads.

(*e*) Specimens made up of mild steel straps (tensile strength 58,000 psi, elongation 26%) and wrought iron plates (tensile strength 52,700 psi, elongation 9.3%), end or side fillet welded. Results are not given in detail but end fillet welds perpendicular to the direction of rolling had the lowest strength, 33,000 to 37,500 psi. Fractures involved the slag fibers in the wrought iron and were of several types. Sahling concluded that craters particularly lowered the strength but undercut, microstructural changes (no details) and shrinkage stresses also had some effect.

Pszenicki and Szelagowski[33]—1936

Bare electrodes, 16 volts, 125 amps. were used. Coated electrodes could not be used because slag was entrapped. Three specimens were averaged for each value in Table 6. Radius of fillets = 2 inches. Base metal was 0.51 inch thick. Mild steel specimens (0.39 inch thick) of the same types were also tested. The greatest difference between individual results in mild

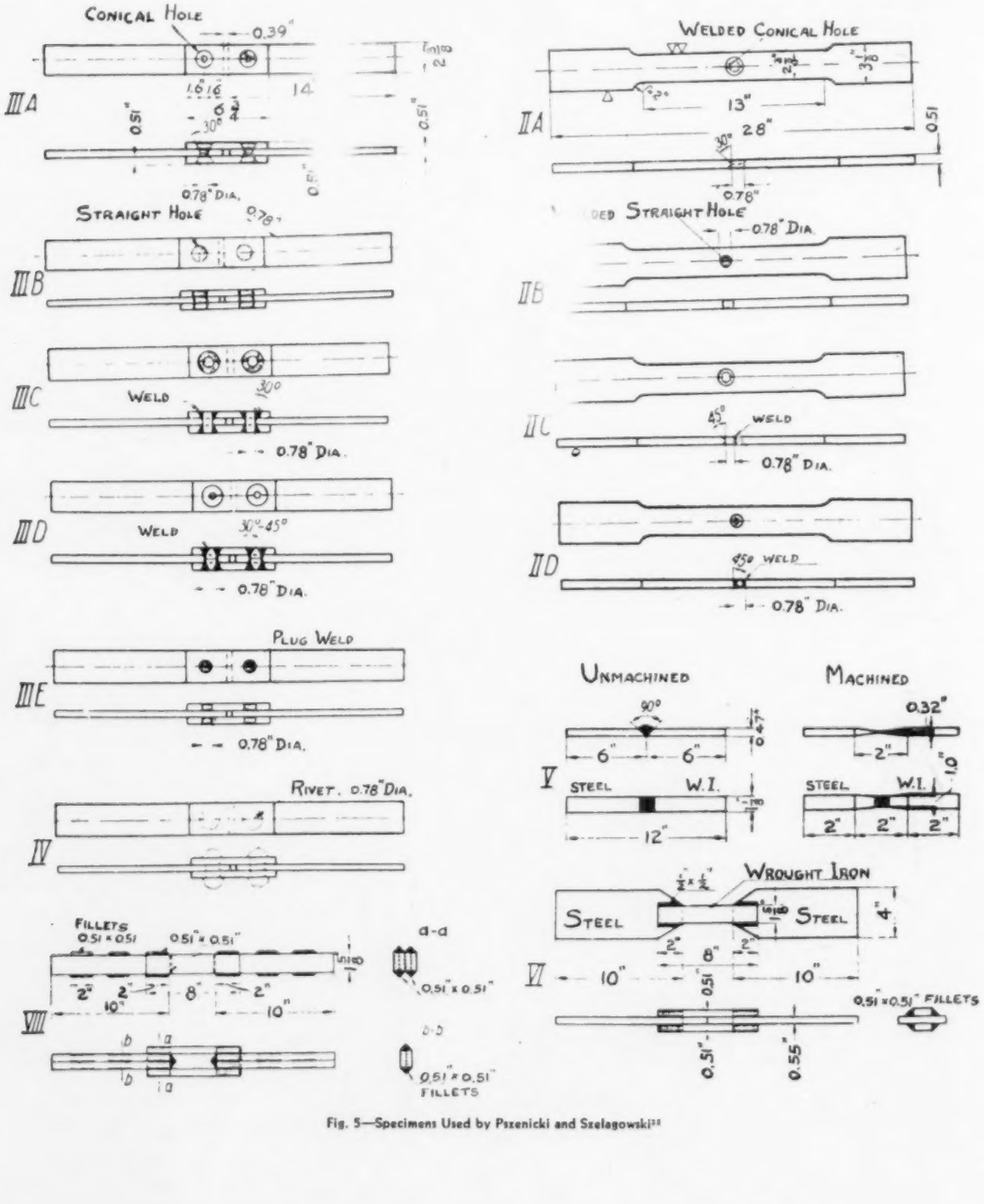

Fig. 5—Specimens Used by Pszenicki and Szelagowski[33]

Table 6—Tensile Tests of Bare Electrode Welded Wrought Iron Specimens Shown in Fig. 1. Pszenicki and Szelagowski[33]

Specimen (Fig. 5)	Yield Point, psi	Tensile Strength, psi	Elongation, %	Remarks
I unwelded	36,800	52,400	17.3	.....
II A	36,600	47,700	10.6	.....
II B	36,300	49,400	6.5	.....
II C	31,100	37,500	2.2	.....
II D	37,100	44,500	5.1	.....
III A	....	41,300	...	Fracture in drill hole.
III B	...	27,200	...	Weld metal sheared.
III C	...	41,700	...	Fracture in drill hole.
III D	...	39,000	...	Fracture outside welds.
III E	...	24,800	...	Weld sheared.
IV	...	28,600	...	Fracture in drill hole.
V	...	62,500	...	.....
VI	...	46,000	...	Fracture in wrought iron bar.
VIII (horizontal weld)	...	23,800	...	Fracture in middle of mild steel bar.
VIII (overhead weld)	...	21,900	...	Two fractured by shear of mild steel along weld; one failed by shear and fracture of strap.

steel specimens of the same type was 11%. For wrought iron the greatest difference was 28% (holes filled with weld metal). The differences were smaller for all other types. The yield point of wrought iron was not much changed by welding. The low elongation of specimens that fractured in the holes was explained by shrinkage stresses (no details). Specimens with a bolt inserted in a central hole and welded in always fractured in base metal. Joint III C was best, Joint III E was worst for wrought iron. For mild steel Joint III C again was the best, but III B had the lowest strength. Specimen VIII was made entirely of wrought iron; similar specimens of mild steel were 25% stronger.

Ellson[34]—1935

The effect of reinforcing rolled wrought iron I beams (10 x 4 1/2 inches, 14 ft. long, 24 lb. per ft.) by welding or riveting flange plates (no analysis) to the flanges was determined by cross bend tests (three-point loading). The plates were 9 ft. long. The plate on the upper flange was 3 1/2 x 5/8 inch; on the lower, 6 x 3/8 inch. Continuous stitch welds (3/4 inch stitches) were run simultaneously on both sides of each plate. The deflection caused by welding the first plate was corrected by the second. There was a residual compressive stress of 8000 psi in both plates after welding. The beam shortened 1/32 inch as a result of welding. Each beam was tested to an ultimate deflection of 1 1/8 inches. The welded beams gave 39,400 to 41,500 psi, calculated on net section. A similar beam reinforced by riveting gave 42,000 psi, the rivet holes (3/4 inch diameter) being deducted from the net section in calculating the stress. The load at 1 1/8 inches deflection was less for the riveted than for the welded. An unreinforced beam gave only 37,000 psi.

Roof[35]—1933

Wrought iron being superior to steel for reinforcing plates on bridges subjected to brine drippings, cross bend tests, Table 7, were made on 4-inch rolled steel H beams (13.8 lb. per ft., 33 inches between support, three point loading) in three conditions:

1. As-rolled.
2. Beam with one flange planed down (reduced) to a thickness of 0.10 inch to simulate the effects of corrosion; the flange thickness as-rolled was 0.29 inch at edge.
3. Reduced-flange beams reinforced by welding wrought iron flange plates.

Table 7—Cross-Bend Tests of Steel H Beams Strengthened by Wrought Iron Flange Plates. Roof[35]

Specimen	Maximum Load, Lb.	Maximum Deflection, Inch	Load at Weld Point, Lb.	Relative Safe Strength	Relative Stiffness
As-Rolled	40,000	3 1/4	20,800	1.00	1.000
Reduced Flange in tension	37,000	3 1/2	22,700	1.09	0.726
Reduced Flange in Compression	36,000	4	19,800	0.95	0.818
R.F.T., 1/2 Inch Cover plate	42,700	1 3/4	28,700	1.38	0.682
R.F.T., 1/2 Inch Cover plate	43,000	2 1/8	27,000	1.30	0.776
R.F.C., 1/2 Inch Cover plate	48,500	3 3/4	23,700	1.14	0.900
R.F.C., 1/2 Inch Cover plate	47,500	2 3/4	25,300	1.22	0.882
R. F. T., 1/16 Inch Cover plate	45,800	2 1/2	29,600	1.42	0.818
R.F.T., 7/16 Inch Cover plate	49,450	3 1/2	25,800	1.24	1.045
R.F.T., 7/16 Inch Cover plate	45,000	2 5/8	19,400	0.93	0.882

R.F.T. = reinforced reduced flange in tension
R.F.C. = reinforced reduced flange in compression.

The wrought iron cover plates were 3 x 24 inches and conformed to A. S. T. M. A. 42–30, Class A. Presumably a low-carbon steel electrode was used (no details); the welds were made downhand. No difficulty was experienced in welding, and micrographs showed excellent penetration and no balling-up of slag. After failure of the beams the welds were found to be intact despite the rupture of the beam in some cases.

Tests of 5/16-inch fillet welds in wrought iron resulted in 9280 lb. per linear inch compared with 9520 lb. per linear inch in steel (no details). An assumed working stress of 2500 lb. per linear inch was used.

Table 8—Tests of Wrought Iron Fillet Welded to Mild Steel. Paton and Grebelnik[36]

Group No.	Length of Weld in Contact with Mild Steel, Inch	Length of Weld in Contact with Wrought Iron, Inch	Area of Contact of Weld with Wrought Iron, Sq. In.	Yield Point, Lb.	Maximum Load, Lb.	No. of Specimens Fractured, In. Plate	No. of Specimens Fractured, In. Weld	Comparison with Group I Area of Contact	Comparison with Group I Yield Point	Comparison with Group I Maximum Load
3	0.28	0.28	2.4	49,800	64,600	12	0	1.00	1.00	1.00
1	0.28	0.39	3.4	50,300	72,000	12	0	1.43	1.01	1.12
2	0.28	0.59	5.1	52,200	79,400	7	5	2.14	1.05	1.23
4	0.28	0.28	3.5	50,500	90,600	16	2	1.45	1.02	1.41

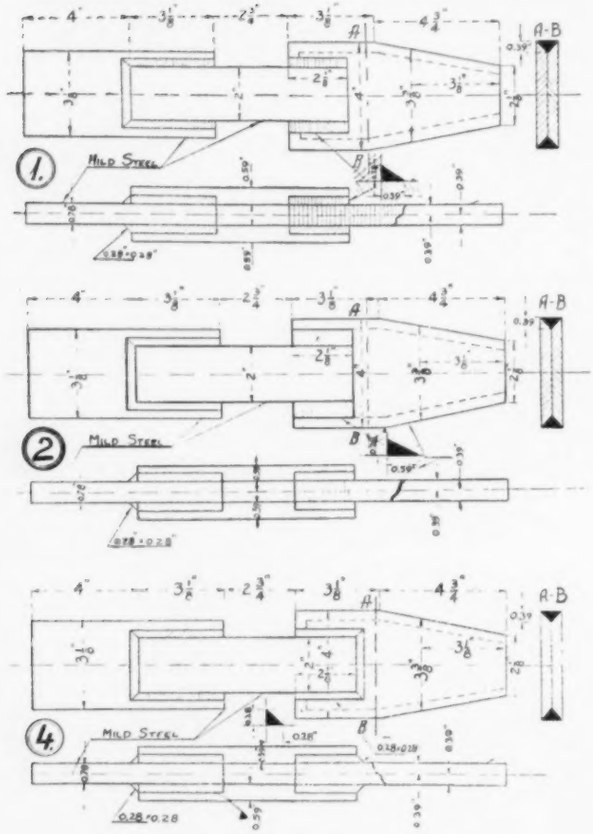

Fig. 6—Specimens Used by Paton and Grebelnik[36]

Paton and Grebelnik[36]—1933

Specimens of the type shown in Fig. 6 were tested in tension, Table 8. The wrought iron plates were composite; that is, two wrought iron plates 0.39 inch thick were placed face to face and welded along the edges. The cover straps were mild steel. All side fillets failed by pulling out the wrought iron in the fusion zone. Specimens of Groups 1 and 3 were identical with Group 2, Fig. 6, except for fillet size. The direction of rolling was parallel to the side fillets. It is evident that end fillets added to side fillets increase the maximum load but do not increase the yield point. It is also clear that the amount of weld metal in contact with wrought iron has a pronounced effect on the maximum load but not on the yield point within the limits of the experiment. Possibly the explanation of these observations lies in the high stress concentrations at the ends of side fillets. Regardless of their size the fillets reach the yield point locally at a critical load. Inelastic action somewhat modifies the stress distribution until the maximum load is reached, which depends on the area of metallic contact of weld metal with wrought iron.

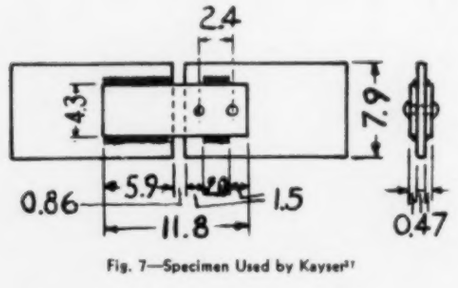

Fig. 7—Specimen Used by Kayser[37]

Kayser[37]—1932

Tests were made on side fillet welded and riveted double-strap joints in wrought iron with wrought iron or mild steel straps. The minimum tensile strength of the mild steel plates and rivets was 53,000 and 48,500 psi, respectively. The rivets were 0.67 inch diameter and had about the same properties as the wrought iron, which had the following properties, average of three specimens 1.02 x 0.47 inch:

Yield Point, 25,200 psi

Tensile Strength, 50,500 psi

Elongation 18.8%. The leg of the weld was 0.32 inch in all specimens. The wrought iron was obtained from an old bridge built in 1861–1862 and was wire brushed before being welded by a qualified welder with bare electrodes, D.C. No welding difficulties were encountered. The results, Table 9, are averages of three specimens

Table 9—Strength of Welded and Riveted Double-Strap Joints in Wrought Iron. Kayser[37]

Type of Joint	Strap	Cross Section, Sq. In. Weld	Rivet	Tensile Strength, psi	Load at Fracture Lb.
Welded Only	Mild Steel	2.1	0	48,000	99,000
Riveted "	" "	0	1.1	62,000	69,000
Welded and Riveted	" "	2.0	1.1	....	124,000
Welded Only	Wrought Iron	2.0	0	45,500	90,500
Riveted "	" "	0	1.1	66,000	73,000
Welded and Riveted	" "	1.9	1.1	....	114,000

each. Stress in the weld was computed on throat area, a distance of 0.20 inch being subtracted from each end to account for crater. Fracture never occurred in the throat; the wrought iron pulled out in the fusion zone. It was believed that fracture in the wrought iron could have been avoided by doubling the leg of the fillet on wrought iron.

Kilp and Gebauer[38]—1931

Specimens shown in Fig. 8 were welded by the workshops of Siemens-Schukert at Nürnberg, Germany. The wrought iron was taken from old wrought iron bridges, about 50 years old. Specimens I, II, V, VI and VII were welded with bare electrodes 0.16 in. diam., 20–25 volts, 120–150 amps. The other specimens were welded with bare electrodes, 0.12 and 0.16 in. diam. The properties of the materials used are given in Table 10.

Table 10—Physical Properties of Materials Used by Kilp[38]

Material	Dimensions, Inch	Used in Group No.	Yield Point psi	Tensile Strength psi
Wrought Iron	0.39 × 3.2	I–IV	32,800	51,000
" "	0.39 × 4.1	V–VII	not determined	47,000
Mild Steel	0.79 × 0.79	II, IV	39,500	58,000
" "	0.79 × 0.79	VII	49,100	73,000
" "	0.39 × 3.9	V	41,800	54,300
" "	0.39 × 3.1	VI	41,000	61,000
" "	0.86 diam.	III	35,200	57,400
Electrodes	0.12 diam.	...	not determined	46,500
"	0.16 "	...	not determined	50,000

In the results shown in Table 11, the values for Groups II, III and IV are averages of three specimens; those for groups V and VII are averages of two specimens. The welds were slightly concave.

In the specimens of Type II (1.34 inch side fillets) failure always commenced in the wrought iron at the end of the mild steel strap. The wrought iron was pulled out along the side fillet.

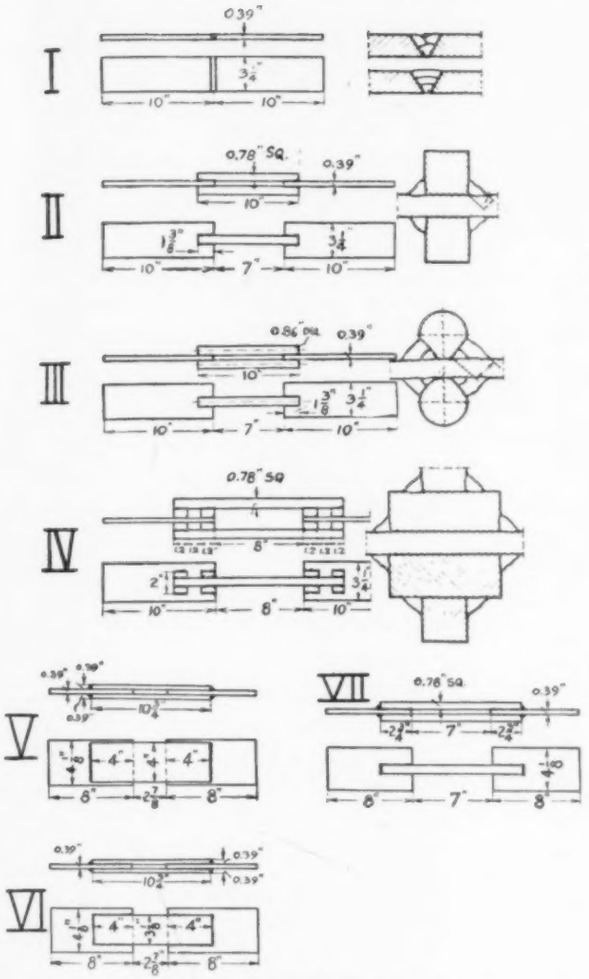

Fig. 8—Specimens Used by Kilp and Gebauer[38]

Table 11—Tests of Welded Wrought Iron. Kilp and Gebauer[38]

Group No. I Yield point = 32,000 psi; tensile strength = 43,100 psi; fracture occurred at the junction between weld and base metal.

Group No.	Cross Section of Weld Sq. In. F_1	Cross Section of Fracture in Wrought Iron Sq. In. F_2	Shear Stress, psi in Weld P/F_1	Shear Stress, psi On Cross Section of Fracture P/F_2
II	1.08	1.52	36,200	26,100
III	2.16	2.28	18,500	17,500
IV	1.12 and 1.30	1.76	27,000	19,900

The weld cross section mild steel to mild steel was 1.12 sq. in. in Group IV; The weld cross section mild steel to wrought iron was 1.30 sq. in. in Group IV; Fracture occurred by tearing away the wrought iron along slag laminations; the direction of rolling coincided with the length of the fillets.

Table 11—(Continued)

Group No.	Total Cross Section of Weld, Sq. In. F_1	Cross Section of Wrought Iron, Sq. in., F_2	Strength of Weld, P/F_1, psi	Tensile Stress in Wrought Iron, P/F_2 psi
V	1.86	1.57	30,700	36,300
VI	5.21	1.55	13,400	45,200
VII	2.98	1.55	21,100	40,400

Group V fractured in weld.
Group VI fractured in wrought iron.
Group VII fracture started at the end fillet weld and spread through the wrought iron. The stress distribution was unfavorable.

In specimens of Types VI and VIII fracture occurred mainly or wholly in wrought iron. The high tensile strength is said to be explained by the eccentricity of loading.

Tensile specimens of the wrought iron with direction of fibers perpendicular to axis of tension fractured at 35,400 psi. It need not be expected that joints welded so as to bring fibers perpendicular to axis of tension will have a correspondingly low strength, if a sufficient number of fibers are gripped by the weld. In discussion, Kloppers pointed out that different results may be anticipated with covered electrodes.

W. D. Chapman[21]—1928

Tensile tests of fillet welds in wrought iron made as first attempts by structural welders using blue asbestos covered electrodes (10 gage) resulted in 4400 to 10,900 lb. per linear inch (no details). Vertical and overhead welds had about the same strength as downhand. The best type of fillet weld to increase the strength across the slag fibers of wrought iron had a leg distance on the wrought iron of $1^1/_2$ times the thickness of the mild steel strap. The following working loads appeared reasonable:

$4^1/_2$ inch weld per $16^3/_4$ inches of electrode: shear = 800 lb. per linear inch
$4^1/_2$ inch weld per $16^3/_4$ inches of electrode: tension = 1100 lb. per linear inch
2 runs; 9 inches and $4^1/_2$ inches per electrode: shear = 1100 lb. per linear inch
tension = 1400 lb. per linear inch
The electrodes were 10 gage.

Wedemeyer[39]—1925

V welds in wrought iron presumably made by the metal arc process had a tensile strength of about 75% of unwelded base metal, which had a tensile strength of 57,000 psi. The specimens were 2 inches wide and 0.24, 0.39 and 0.79 inch thick.

Oxyacetylene Welded Wrought Iron

Investigator

Daeves and Rädeker[41]—1936

Wrought iron containing 0.03 C, 0.124 P, 0.14 Cu and having the average mechanical properties shown in Table 12 was used.

Table 12—Mechanical Properties of Wrought Iron Used by Daeves and Rädeker[41]

	As Received		Annealed to 930° C.	
	Parallel to Direction of Rolling	Perpendicular to Direction of Rolling	Parallel to Direction of Rolling	Perpendicular to Direction of Rolling
Yield Point, psi	32,000	31,500	32,700	34,000
Tensile Strength, psi	48,500	44,500	48,300	47,000
Elongation, %	15.2	7.9	16.4	6.9
Notch Impact Value, Mkg./Cm.^{2}	6.5	4.3	7.9	4.8

Two conical holes, 0.79 inch diameter in 1-inch plate were filled with:

1. Rod forged from wrought iron plate
2. Mild steel rod, by means of the oxyacetylene process. The microstructure was satisfactory.

The tensile properties of the welded specimens are given in Table 13.

Table 13—Tensile Tests of Oxyacetylene Welded Wrought Iron. Daeves and Rädeker[41]

	Unannealed		Normalized	
	Wrought Iron Rod	Mild Steel Rod	Wrought Iron Rod	Mild Steel Rod
Yield Point, psi	28,200	....	30,600	....
Tensile Strength, psi	34,500	18,800	45,700	29,200

The mild steel welds, being harder than base metal, did not share in the tensile elongation.

Kayser[10]—1930

Symmetrical, double strap joints, end or side fillet welded, were prepared from old wrought iron having a yield point = 39,200 psi, tensile strength = 52,500 psi, elongation = 9.4% in 10 diameters. The welds were made with some difficulty (oxygen-acetylene ratio = 1.2) with a copper coated mild steel rod. Weld metal contained 0.15% Cu. The plates for the side fillet welded joints were 0.28 inch thick; the welds were 1.18 inches long, leg = 0.28 inch. The straps were 2.16 inches wide. The joints were calculated for 34,100 psi but actually developed 45,300 psi.

The plates and straps for the end fillet welded joints were 0.24 to 0.28 inch thick; the welds were 1.18 inches long, leg = 0.26 inch. Calculated for 42,700 psi, the end fillets actually failed at 69,500 psi.

Stanton and Pannell[40]—1911

Oxyacetylene welded butt joints in wrought iron made with mild steel rods by a number of firms had 50 to 80% of the tensile strength of base metal, 0 to 10% of the elongation and 30 to 60% of the rotating cantilever fatigue strength.

Bach and Baumann[42]—1910

Gas welds of mild steel to wrought iron did not show much ductility.

Resistance Welded Wrought Iron

Investigator

H. A. Woofter (private communication, August 1937)

Properly made flash welds in wrought iron will always be at least 10% stronger at the weld than in base metal.

Blomberg[19]—1937

The tensile strength of resistance welds by three processes is shown in Table 14.

Table 14—Tensile Strength of Resistance Welded Wrought Iron. Blomberg[19]

Resistance Butt Weld, Manual Operation, No Fusion	36,000 psi
Flash Weld, Manual Operation	36,000 psi
Flash Weld, Machine Operation	41,300 psi

There was slag along the junction of the resistance butt weld, together with considerable porosity. The manual flash well showed holes at and near the junction. The machine flash weld showed very little porosity.

Nápravník and Popov[16]—1935

The mechanical properties of hand and resistance butt welded wrought iron in the form of straightened-out links are given in Table 15.

Table 15—Mechanical Properties of Wrought Iron Used by Napravnik and Popov[16]

Yield Point	33,100 psi
Tensile Strength	50,800 psi
Reduction of Area	64.7%
Elongation	25.7% in 2 inches
Notch Impact Value	11.1 mkg per cm.^{2}

The wrought iron contained 0.028 C, 0.11 Mn, 0.011 Si, 0.032 P, 0.014 S, 0.32 Cu. The impact specimen was 0.35 x 0.35 x 2.2 inches, 0.05 inch notch diameter, 0.17 inch deep. The tensile specimens were machined, $^{13}/_{64}$ or $^{9}/_{32}$ inch diameter. The links were hot straightened, and annealed $^1/_2$ hr. at 940° C. before being machined.

The strength of chains 90 feet long is given in Table 16, the test loads being 6900 and 12,100 lb.

Table 16—Tests of Wrought Iron Chain. Napravnik and Popov[16]

Method of Welding	Diameter of Chain, Inch	Tensile Load, Lb.	Elongation, %	Number of Broken Links
Hand Forge Welded	0.47	6,900	10.50	0
Resistance Butt Welded	0.50	6,900	5.52	2
Hand Forge Welded	0.63	12,100	9.75	0
Resistance Butt Welded	0.65	12,100	6.20	2

Bastänier[43]—1922

Wrought iron bars $1^3/_4$ inches diameter were resistance butt welded and tested as shown in Table 17. The reinforcement was hammered flat before testing:

Table 17—Tests of Resistance Welded Wrought Iron. Bastänier[43]

Specimen	Tensile Strength, psi	Percentage of Tensile Strength of Unwelded Wrought Iron
Reinforcement Hammered	40,000	82.1
Reinforcement Machined Off, Not Hammered	45,000	92.4

Stanton and Pannell[40]—1911

Resistance butt-welded joints in wrought iron made by a number of firms had 90 to 100% of the tensile strength of unwelded base metal, 0 to 150% of the elongation of base metal, and 86% of the rotating cantilever fatigue strength. Resistance butt welding was better for $1^1/_4$-inch bars than for $^1/_2$-inch bars.

Dobson[44]—1894

Resistance butt welds in wrought iron bars $1^1/_8$ inches diameter were cold bent 34 to 150° without fracture.

Woodbury[69]—1888

Watertown Arsenal made a large number of tensile tests on machined and unmachined resistance butt welds in wrought iron (tensile strength 52,000 to 64,000 psi). Many of the specimens fractured at some distance from the weld. Some had 100% strength or more, whereas the tensile strength of others was only 70 to 80% of unwelded base metal (no explanation).

Applications

Old Bridges

Of the many applications of welded wrought iron, most attention has been paid to the reinforcement of old, weak or corroded wrought iron bridges by means of welding. The reinforcing material is mild steel. The

principles of wrought iron bridge repair and reinforcement are clearly stated by McKibben[12,45] who cites a number of examples. The old wrought iron must be thoroughly cleaned by scraper or torch to expose clean metal of sufficient thickness for welding. Several examples of wrought iron bridges reinforced by arc welding are described by Bruff.[46] A few fractures are discussed that were due to welding or service stresses in the reinforced bridges.

Sahling[32] also discusses the structural aspects of reinforcing wrought iron bridges. In agreement with Bruff, he found only 3 feet of cracks in 105,000 feet of welds. Cracks mainly occurred in corner stiffening plates between vertical members and floor beams especially at short verticals near the end of a bridge. When the floor beams deflect under load the stiffening plates are highly stressed. Cracks also appeared in the welds joining wind connections to the lower main girder. Although Sahling states that the cause of cracks in the wind connections is unknown, it is at least possible that the explanation may lie in the great difference in cross section between connection and main girder. Some small reinforced girders were found to have arched 0.16 inch in the middle after welding. Sahling found that the lateral amplitude of vibration of a wrought iron bridge was decreased 45% by welded reinforcement; the deflection was decreased 5% as shown by pulsator tests.

The specifications for Highway and Railway Bridges of the American Welding Society (1936) state that in the application of wrought iron to bridge repair, the iron must be so placed that the direction of maximum stress must coincide with the direction of rolling. According to Railway Age,[47] the first welding on wrought iron bridges in this country was in 1927. In the same year Hopkins[48] stated that there was no difference between welding old wrought iron bridge work and open hearth steel. Since 1927 the strengthening of wrought iron bridges by welding has been common practise.[22,33] Ellis[49] cites a wrought iron bridge hanger that was demolished by a heavy truck, the iron being torn through at two places. Yet the welds at the end of the hanger showed no signs of failure.

Miscellaneous Applications

Although the chief application of welded wrought iron that has been discussed in the literature is the repair of old wrought iron bridges, the major applications are probably gas, arc and flash welded wrought iron pipe and fittings. Fritz[50] and Holler[51] describe the welding of wrought iron plumbing. Trinham[52] and Jurczyk[53] recommend the welding of wrought iron gas and water pipe. Holler[54] gives a complete description of the design of welded heating radiators made of wrought iron pipe, and *Boiled Maker*[2] illustrates the application of welding to wrought iron sewage pipes and syrup tanks.

A prominent application of welded wrought iron is in boiler parts. Bothmer[55] found no difficulty in flame cutting and oxyacetylene welding wrought iron for small boilers. A clever job of welded wrought iron boiler breeching is illustrated by Landes.[2] Woodroffe and Lester[56] provide general information on the welding of charcoal iron locomotive boiler tubes. Wrought iron boiler parts were oxyacetylene welded by the Pintsch firm[57] in 1910. A variety of arc-welded wrought iron products, such as air valves and chutes, are described by Haspel,[58] and Voigtländer[59] showed the advantages of flame cutting and oxyacetylene welding over other methods in the production of wrought iron rings 15 inches outside diameter, $1^1/_8$ inches thick, $1^1/_8$ inches long.

Wrought iron is extensively used on bridges for protection of other members from corrosion and for welded railings. *Railway Age*[60] describes a welded wrought iron deck ($^5/_8$-inch plate) for a railroad trestle and also illustrates[61] the application of butt and fillet-welded wrought iron plates to the protection of a concrete bridge pier from ice at high water. Wrought iron was selected on account of its corrosion resistance and is also used in welded lock wall protection and for welded trash racks for water power head gates. A report of the Engineering Service Department of A. M. Byers Co. (1934) gives several examples of the use of welded wrought iron for the decks of ballast-type bridges and for blast plates on railway viaducts and tunnel roofs. Welding has been used recently in the construction of wrought iron trawler hulls.

According to H. A. Woofter (private communication, August 1937) resistance butt welding is used with entire success in producing perhaps 99% of all wrought iron chain in this country, the other 1% being cast chain for anchorage use. Small chain may be welded at the rate of 67 links per minute. Experience has shown that welded wrought iron chain does not deteriorate so rapidly in sea water as steel chain.

Welding Wrought Iron to Other Metals

There appears to be no difficulty in welding wrought iron to mild steel by arc[31] or gas[62] processes (see section on Mechanical Properties). The difference in melting points between the two metals appears to be immaterial and the absence of slag in the mild steel should aid, not hinder, the welder. Wrought iron has been metal arc welded to high speed steel[63] (V butt joint). It has also been successfully welded to cast iron[64] by means of a covered mild steel electrode. A resistance weld between wrought iron and german silver[44] was exhibited in 1894.

The use of wrought iron as a filler rod for steel was common practice in the early days of welding. S. W. Miller[65] stated that when oxyacetylene welding was taken up in this country in 1904 the filler rod was wrought iron from Norway or Sweden. The wrought iron slag protected the weld metal from excessive oxidation. As Keel[66] points out, a wrought iron rod is too weak to give a weld of sufficient strength in modern steels. Coated wrought iron electrodes have often been recommended for cast iron with the object of reducing the tendency to hard zones.

Flame Cutting

It is generally considered[72] that there is no difficulty whatever in flame cutting wrought iron using the same procedure as for mild steel. An anonymous writer[67] found that flame cutting wrought iron with the oxy-hydrogen torch did not alter the slag inclusions but slightly refined the grain at the cut edge. On cooling immediately after cutting, cracks originated at large slag inclusions and spread parallel to the direction of rolling. The plate was 24 inches thick and it was believed that the transverse strength and ductility of the wrought iron was insufficient to permit it to resist the shrinkage stresses. The Brinell hardness of base metal was 101, of the cut surface 107. No cracks were found in plates up to 12 inches thick. The use of the carbon arc in burning holes in wrought iron plates was described by Auel.[68] A hole $1^3/_4$ inches diameter, $1^1/_2$ inches deep was burnt in $3^1/_2$ minutes (including 45 seconds for reversing the plate) by means of a carbon electrode $1^1/_2$ inches diameter, 370–430 amps.

Acknowledgments

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U. S. Navy Bureau of Construction and Repair.
H. A. Woofter, Federal Machine and Welder Company.

Appendix—Average Properties of Wrought Iron

Chemical Composition, weight % (Aston and Story: "typical analysis")

	Combined Analysis	Separate Analysis Showing Distribution of Constituents	
		Base Metal	Slag
Carbon	0.02 (up to 0.10)	0.02	...
Manganese	0.03	0.01	0.02
Phosphorus	0.12 (up to 0.15)	0.10	0.02
Sulphur	0.02 (less than 0.03)	0.02	...
Silicon	0.15 (0.10–0.20)	0.01	0.14
Slag	3.00 (1–3)	...	...

Nearly all A. S. T. M. standards restrict the manganese content to 0.06%. Copper up to 0.25% and nickel up to 3% are sometimes added.

Physical Constants

Coefficient of Thermal Expansion (18 to 100° C.)	0.0000114
Thermal Conductivity (20° C.) (gram-calories per second per square centimeter per degree Centigrade per centimeter)	0.144
Specific Gravity	7.70
Melting Point	Approximately 1500° C.

Mechanical Properties

Short-Time Tensile Strength, psi	20° C.	48,000 (longitudinal)
	20° C.	35,000 (transverse)
	400° C.	40,000 (longitudinal)
	600° C.	14,000 "
	800° C.	4,000 "
Short-Time Yield Point, psi	20° C.	30,000 "
	400° C.	26,000 "
Elongation, % in 8 Inches	20° C.	25 "
Elongation, % in 2 Inches	400° C.	23 "
	600° C.	30 "
	900° C.	10 "
Reduction of Area, %	20° C.	45 "
Young's Modulus, psi		29,000,000
Hardness		B 55 (Rockwell) 95–105 Brinell
Charpy Impact Value, Ft.-Lb., 20° C.		longitudinal, up to 50 transverse, as low as 5
Rotating Bend Fatigue Limit, psi		longitudinal, 23,000 transverse, 19,000

Bibliography

NOTE: *For the reader's convenience the most extensive research investigations on welding wrought iron are designated by an asterisk in the bibliography.*

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Suggested Research Problems in Welding Wrought Iron

1. Impact and fatigue properties of arc- and gas-welded wrought iron.
2. Properties of bronze-welded wrought iron.
3. Welding of wrought iron alloyed with nickel (metallurgy and testing).
4. Welding of galvanized wrought iron (procedure and properties).
5. Metallography and properties of flash-welded wrought iron.
6. Strength of spot welds in heavy wrought iron plates (1/4 inch to 1/2 inch thick).
7. The slag question in welding wrought iron with covered electrodes? Do some coatings tend to entrap slag in the weld, as stated by reference 33.
8. The relationship among power, pressure, upset, slag content and mechanical properties and structure of resistance butt-welded wrought iron.

Mr. B. D. Landis has made some tensile and shear tests and chemical analyses of arc welds in wrought iron which will be reproduced in a forthcoming issue of THE WELDING JOURNAL. *Unfortunately his data were received too late for inclusion in the present review.*

The Effect of Total Carbon and Manganese on the Mechanical Properties and Structure of Welded Joints in Plain Low-Carbon Steel

A Review of the Literature to January 1, 1937

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This Report Is Prepared Under the Auspices of the Literature Division of the Engineering Foundation Welding Research Committee

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INTRODUCTION

Manganese is one of the alloying elements in steel that promote air hardening. That is, manganese lowers the critical cooling speed required to produce martensite. Air hardening in welds, especially if excessive, is usually dangerous because the air-hardened region is brittle and because the low-temperature martensite expansion favors cracks. In some kinds of welding, depending on plate thickness, and atmospheric and welding conditions, the local cooling rate at some point in weld or heat-affected zone, is such that, given a rolled, plain low- or medium- carbon-manganese steel with above a critical content of carbon or manganese (up to say, 0.4% C or 3% Mn which are the limiting contents considered in this review), air hardening occurs. The present review of literature was undertaken at the request of the A. S. T. M. Committee on Plain Carbon Steels, which is planning a program of research on the interlocking effects of carbon, manganese and welding procedure on mechanical and metallographic properties (weldability). The review was begun in the hope that a study of all published experimental work on the mechanical properties and microstructure of welds in plain carbon-manganese steels might reveal that there were critical contents of carbon to manganese in the weld and base metal that could be depended upon to lead to air hardening or brittleness under given welding conditions. For brittleness may be induced in welded joints by other factors than air hardening, for example, nitrides and coarse grain structure. The hope has not been realized owing to the extreme diversity in welding conditions prevailing in the different experiments and to the absence of any systematic investigation of the carbon-manganese contents apart from other alloying elements. The review does not deal with the effect of manganese on the molten weld puddle.

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SUMMARY

Mechanical Properties of Welded Joints

It is not unexpected that welds in steels containing high manganese and carbon have higher tensile strengths than low-carbon, low-manganese welds. Undoubtedly, differences in welding procedures among the investigators account for the differences in mechanical properties rather than differences in carbon and manganese content. No comprehensive investigation has been made of the effect of different combinations of carbon and manganese on the mechanical properties of welds. The heat-affected zones of non-stress relieved single layer deposits on steels containing 0.33 and 0.44% C contained large amounts of martensite. With one layer welding these steels gave low notch impact values—in the latter case the value was practically reduced to zero. With four layers there was little martensite.

Hardness

The maximum hardness increases with increasing content of manganese and carbon, as well as with increase of welding speed. The speed factor is very important. As welding speed increases, the maximum grain size in the heat-affected zone decreases. The hardening power for manganese is much less that that of carbon and depends, at least in part, upon the carbon content. Further work is considered essential before a final solution of the carbon-manganese question can be reached.

The mechanical properties of a welded joint are not directly related to the maximum hardness. On the other hand, the "weld quench" obtained in a single bead appears to be a significant indication of the limitations which must be placed on welding variables, such as plate thickness and welding speed, in order to avoid dangerously high hardness.

General Observations

Manganese, within the usual limits found in commercial plain carbon steels, imposes no special requirements

in welding, so long as the carbon content of the steel is low (below 0.25% C). With increase in carbon content, and depending upon the amount of manganese present, especially around 1% or more, air hardening properties come into play and precautions, such as preheating, may be required to eliminate or reduce the tendency toward martensite in the heat-affected zones. Gas welds in thin tubing containing 0.3 C, 1.75 Mn are likely to be brittle. A steel containing 0.42 C, 1.1 Mn failed in the Fokker test (bead of oxyacetylene weld metal deposited from edge to center of a square sheet 0.047 inch thick) whereas a steel containing less than 0.2 C, and 2.25 Mn did not. Welding cracks bear no relation to manganese content up to 0.55%. On the contrary there is said to be greater difficulty in avoiding welding cracks if the manganese content is between 0.3 and 0.6% than with higher or lower manganese content.

Manganese does not appear to be an important factor in nitrogen pickup. Drums with 0.42 to 0.51 C, 0.93 to 1.39 Mn had a strong tendency to cracking during flame hardening. Drums with 0.2–0.33 C, 0.55–0.69 Mn gave no trouble.

The Effect of Manganese in Steel

A brief survey of the effects of manganese in medium-manganese steels conclusively proves that the outstanding characteristic distinguishing manganese from the other alloying elements is the powerful effect on hardenability, that is, on the austenite transformation. Manganese exercises a powerful effect in lowering the critical cooling rate necessary to suppress the formation of ferrite and carbide on cooling from above A3. In other words, manganese is fundamentally an air-hardening element. In carbon-free alloys the effect of manganese on the austenite transformation is different from its effect in steels.

Aside from the effects of manganese on hardenability and also on critical points, there is no other characteristic of the element that would appear to have a strong action on the welding of medium-manganese steels, with the exception, of course, of the effects of manganese on the molten weld puddle (slag fluidity and melt reactions).

The strength of these steels at elevated temperature appears to be consistent with their high strength at room temperature. The steels are not specially sensitive to improper heat treatment, but their toughness is appreciably lower than that of other alloy steels having the same strength (above some tensile range, e.g. 70,000 psi).

Review of Literature on the Effect of Total Carbon and Manganese on the Mechanical Properties and Structure of Welded Joints in Plain Low-Carbon Steel

Mechanical Properties of Welded Joints

The effect of carbon and manganese on the mechanical properties of fusion welded joints is summarized in Table 1. The table summarizes relatively recent investigations on welds in steel containing up to 0.4 C, 3.0 Mn without significant quantities of alloying elements. As a matter of fact, many of the steels contained appreciable quantities of silicon and copper, elements which when present in small amount have relatively little effect on the critical cooling rate for martensite. All steels containing appreciable quantities of air-hardening elements (over 0.10–0.5% depending on the element), such as chromium, molybdenum, nickel and vanadium, were excluded from consideration. The steels are listed in decreasing order of the product Mn x C. In view of Theisinger's discovery[17] that the "hardening power" of manganese is apparently $^1/_7$ that of carbon, the steels were listed in decreasing order of $C + \frac{Mn}{7}$. However, there was little difference in the order of the steels in the two lists. The ratio $\frac{Mn}{C}$ appeared to have no significance. The sulphur content of the steels was not considered in calculating the sums and products, although each unit of sulphur satisfies 1.72 units by weight of manganese. The majority of investigators did not state whether tensile fracture occurred in weld or plate, or whether the analysis of the filler rod referred to deposited metal.

It is not unexpected that welds in steels containing high manganese and carbon had higher tensile strengths than low-carbon, low-manganese welds, provided that the weld was at least as strong as the plate. The table, however, does not reveal any trend toward lower ductility or impact values with variation in carbon and manganese. Undoubtedly, differences in welding procedures among the investigators account for the differences in mechanical properties rather than differences in carbon and manganese content. No comprehensive investigation has been made of the effect of different combinations of carbon and manganese on the mechanical properties of welds.

In addition to the investigations listed in Table 1, several others have reported results on carbon and manganese in welds. For example, Blackwood[18] found as a result of tests of all-weld-metal deposited by electrodes containing up to 0.71 Mn and 0.10 C with 0.05 Si, that increase in manganese had no effect except on tensile strength. Söhnchen and Kleinefenn's study[19] of notch impact value at −50 to 400° C. of all-weld-metal deposited by bare and coated electrodes, and by acetylene seemed to show that manganese up to about 1% had no effect which was not completely masked by other elements which were present in their welds. With dipped electrodes or oxyacetylene welding manganese up to 0.75% has no effect, according to Fry,[20] who does not supply details of experimental results.

Lohmann and Schulz[21] found that arc welds made with bare and coated electrodes in steel containing 0.26 C, 0.60 Mn, 0.22 Si, 0.028 S had relatively low ductility, but, as with many other investigators, their results may be accounted for by the nitride question and by loss of elements in welding. Thompson[22] reports the results shown in Table 2 for oxyacetylene welds in Bessemer steel pipe. The higher carbon rod gave 10,000 to 12,000 psi higher tensile strength. Micromachine tests on welded steel containing 1% Mn, less than 0.2% C are reported by Brillié and Séférian.[23]

A repeated impact test (38 blows a minute) made by Taylor and Jones[24] on a 7-inch I beam to which a $^5/_8$-inch plate of the same analysis (0.28 C, 1.0 Mn, 0.25 Si) had

Table 2—Bend Ductility of Oxyacetylene Welds in Pipe. Thompson[22]

Analysis of Rod				Analysis of Weld Metal			Bend Elongation % in 2 Inches
C	Mn	Mn x C	Si	C	Mn	Si	
0.05	0.07	0.004	0.01	0.05	0.07	0.01	22
0.17–0.20	0.40	0.08	0.50	0.14	0.40	0.25	18

been fillet welded to the tension flange resulted in failure at 9690 blows, a disappointingly low figure. The stress was 79% of ultimate. A test at 40% of ultimate did not produce cracks in the beam after 210,000 cycles. The hardness of base metal was Rockwell B84 (159 Brinell); the hardness at fusion line (Lincoln 85 electrodes) was Rockwell B 95 (207 Brinell). Mechanical properties of arc-welded Man-Ten (0.35 C, max., 1.25–1.70 Mn, 0.15 Si min., 0.20 Cu min.) are reported by Williams[25] as satisfactory, but essential experimental details are not given. A micrographic study of arc welds in Cromansil (0.20 C, 0.92 Si, 0.49 Cr, 1.27 Mn) by Gibson[26] showed no martensite as welded. Others have reported similar results for similar low-alloy steels.

Surfacing (Beads on Surface)

The effect of the carbon and manganese content of base metal or weld metal on the maximum hardness in the vicinity of one or more beads deposited on a surface has been studied for two reasons: (1) To secure fundamental information on the "weld quench;" (2) To improve the wearing qualities of welded surfaces. Both types of investigations are conducted in the same way. Beads of weld metal are deposited under controlled conditions so that conditions of cooling in welded joints are reproduced by simple means, as Yatsevitch[27] pointed out. The cross section of the sample is subsequently explored with a Vickers diamond pyramid hardness tester. In this way the maximum hardness, which at the same time is an indication of the limiting values of tensile strength and, perhaps, of ductility in the vicinity of the bead is determined.

Fundamental studies of hardness in the vicinity of surfacing have been made by Theisinger[28] and Meunier and Rosenthal.[29] Theisinger's first series of experiments was made on steels SAE 1015, 1025, 1035, 1045, 1050. The steels were not analyzed for manganese and were used as rolled. The plate to be tested, which rested on a cold steel plate $^3/_4$ x 4 x 12 inches, was $^1/_2$ inch thick and the bead was deposited by A.C. automatic machine along the center of the 2-inch width for the entire length (9 inches) of the plate. Bare electrodes $^5/_{32}$ inch diameter (composition not stated) were used at 23 volts, 275 amps. The same kind of electrodes was used in all tests. A sound specimen as revealed by X-ray was selected for hardness measurements, the results of which are shown in Table 3.

The maximum hardness increases with increasing content of manganese and carbon, as well as with increase of welding speed. The speed factor is very important. There is a smaller increase of hardness at all speeds in SAE 1025 ($C + \frac{Mn}{7} = 0.32$ to 0.36) than in SAE 1015 ($C + \frac{Mn}{7} = 0.21$ to 0.26). There seems to be no explanation since the as-rolled plates in all steels had about the same grain size. The maximum increase in hardness in Brinell units at 9 inches per minute is 37, 18, 29 and 41 for the four steels, the steel with the lowest content of carbon and manganese, SAE 1015, being out of its proper order in the series. If the difference were expressed in per cent of base metal hardness, SAE 1015 would be still farther out of order. The exceptional behavior of SAE 1015 in Theisinger's experiments must be related in some way to the low carbon and manganese content. With this exception the results follow expectations and provide quantitative values of the weld quench effect.

Table 3—Maximum Vickers Brinell Hardness of Steels with a Single Arc Deposited Bead. Theisinger[28]

SAE Steel	Carbon Content %	Manganese Content %	Vickers Brinell Hardness Maximum	Location	Base Metal	Welding Speed, In./min
1015	0.17	0.30–0.60	151	Zone (1)	120	4
"	"	"	158	Zone (1)	121	6
"	"	"	160	Zone and Weld (1)	121	8
"	"	"	174	Weld (1)	120	12
"	"	"	215	Weld (2)	120	18
1025	0.25	0.50–0.80	148	Zone (1)	130	4
"	"	"	148	Zone (1)	130	6
"	"	"	151	Zone and Weld (1)	134	8
"	"	"	174	Weld (2)	134	12
"	"	"	221	Weld (2)	131	18
1035	0.33	0.50–0.80	162	Zone (1)	139	4
"	"	"	168	" (1)	139	6
"	"	"	177	" (1)	140	8
"	"	"	212	" (2)	140	12
"	"	"	250	" (2)	140	18
1045	0.44	0.50–0.80	218	Zone (2)	177	4
"	"	"	218	" (2)	177	6
"	"	"	240	" (3)	177	8
"	"	"	266	" (3)	177	12
"	"	"	333	" (4)	177	18

Zone signifies the heat-affected zone.
(1) Ferrite and coarse pearlite.
(2) Sorbite (fine pearlite).
(3) Troostite (extremely fine pearlite).
(4) Martensite.

Note: It is not to be inferred that the maximum hardness values given in this table are necessarily the maximum values which can occur in metal arc welding $^1/_2$ inch thick carbon steel plates of the compositions indicated.

Micrographic examination showed that as the welding speed increased, the maximum grain size in the heat-affected zone decreased. Probably the corresponding decrease in breadth of the zone as well as the steeper temperature gradients with higher welding speeds explain the grain size observations. The heat-affected zone of SAE 1045 at 18 inches per minute consisted partly of martensite. Theisinger's micrographs (at 150X) do not clearly show the troostite at lower welding speeds. A study of steel containing 0.43 C in thicknesses of $^1/_2$ to 2 inches at a welding speed of 8 inches per minute showed that the maximum Brinell hardness in heat-affected zones rose from 254 at $^1/_2$ inch to 300 at 1 inch and 312 at 2 inches. Beyond 1 inch, therefore, the increasing mass effect had little effect on maximum hardness.

The second series of experiments by Theisinger[17] was performed on the normalized steels shown in Table 4.

Table 4—Steels Used in Theisinger's[17] Second Series of Tests

Steel	$C + \frac{Mn}{7}$	Carbon %	Manganese %
1	0.27	0.19	0.54
2	0.23	0.16	0.50
3	0.33	0.25	0.55
4	0.46	0.35	0.77
5	0.45	0.33	0.81
6	0.39	0.25	0.98
7	0.34	0.19	1.01
8	0.53	0.34	1.32

Table 1—The Effect of Carbon and Manganese on the Mechanical Properties of Metal Arc

Table 1 (A)—

BASE METAL													ELECTRODE											
Thickness, Inch	Mn × C	C	Mn	Mn/C	Si	S	Other	Tensile Strength, psi	Tensile Elongation, %	Bend Ductility, ° or Angle	Impact Value	Brinell Hardness	Diameter, Inch	Type	Amp.	No. of Layers	Mn × C	C	Mn	Mn/C	Si	S
0.10	0.462	0.42	1.10	2.6	0.30	Ditto	..	103,000	26.0	>180°	..	..	..	Light coated	....	..						
0.10	<0.450	<0.20	2.25	>11	..		..	110,000	19.3	180°			..	Austenitic	....	..						
0.24	"	"	"	"	..		..	112,000	19.2	100°					....							
0.47	"	"	"	"				113,000	17.8	65°												
0.24	"	"	"	"				112,000	19.2	100°				Alloy Cored Electrode								
0.50	0.375	0.25	1.5	6.0	..	..	..	85,300	..	..	..	..	..	Covered	....	..						
"	"	"	"	"				84,000													Mild Steel No. 1	
"	"	"	"	"				"													" "	No. 2
0.47	0.326	0.18	1.81	10.0	0.38	..	0.48 Cu						0.16	Cored	150	..						
0.39	0.262	0.18	1.45	8.1	0.15	..	0.28 Cu			180°	10.6	159	Shielded Arc, Also Freely Flowing Type									
0.47	0.248	0.21	1.18	5.6	0.50	..	0.58 Cu										0.03	0.07	0.43	6.2	..	0.05
"	"	"	"	"	"		"										0.031	0.07	0.44	6.3	"	0.04
"	"	"	"	"	"		"										0.063	0.09	0.70	7.8	0.11	0.04
"	"	"	"	"	"		"										0.050	0.10	0.50	5.0	0.10	0.04
0.47	0.244	0.38	0.64	1.7	0.29	0.018		92,000	20.2	>180°	7.3	165	Heavy Coated Low-Alloy Structural Steel									
"	0.244	0.20	1.22	6.1	0.71		0.10 Mo 0.42 Cu						0.16	Cored	150							
0.78	0.244	0.22	1.11	5.1	1.01	0.10 Mo 0.02	0.47 Cu 0.05 Cr	92,000	31			156–176		Coated			0.055	0.11	0.50	4.6	0.05	0.02
0.32	0.229	0.21	1.09	5.2	0.58	0.03	0.46 Cu 0.38 Cu	81,000						Cored			0.063	0.09	0.70	7.8	0.11	0.03
	0.226	0.19	1.19	6.3	0.72																	
0.39	0.220	0.20	1.10	5.5	0.48	0.10 Mo 0.02	0.44 Cu 0.11 Cr	87,000	26			156–176		Coated			0.055	0.11	0.50	4.6	0.05	0.02
..	..	..	..	..	..	..	..							Bare			0.065	0.09	0.72	8.0	0.05	0.02
0.47	0.218	0.20	1.09	5.5	0.80	0.018	0.30 Cu	90,000				166		Cored		3	0.059	0.09	0.65	7.2	0.10	0.02
"	"	"	"	"	"	"	"					"		"		"	0.139	0.115	1.21	10.5	0.26	0.02
"	"	"	"	"	"	"	"					"		"		"	0.580	0.29	2.00	6.9	0.14	0.02
"	0.204	0.18	1.13	6.3	1.23		0.40 Cu						0.16	Cored	150							
0.39	0.192	0.17	1.13	6.7	0.94	0.03	0.29 Cu										1.00	0.55	1.81	3.3	0.24	0.02
"	"	"	"	"	"	"	"										0.14	0.17	0.82	4.8	0.25	0.02
0.39	0.189	0.23	0.82	3.6	0.28	0.025	None	86,000	26.6	74%	7.2	175					0.14	0.15	0.90	6.0	0.61	0.02
																5	0.03	0.08	0.36	4.5	0.01	0.02
0.47	0.172	0.24	0.72	3.0	0.98		0.31 Cu						0.16	Cored	150							
0.47	0.168	0.30	0.56	1.9	0.23	0.040	None	84,000	22.4	>180°	7.1	152		Heavy Coated	190		0.050	0.10	0.50	5.0	0.10	
0.28	0.167	0.21	0.75	3.6	0.03	0.029	"	69,000						Bare		3	0.016	0.04	0.40	10.0	Trace	0.02
0.47	0.166	0.16	1.04	6.5	1.01	0.054	0.39 Cu	80,000						Cored		..	0.059	0.09	0.65	7.2	0.10	0.02
"	"	"	"	"	"	"	"	"						"		..	0.139	0.115	1.21	10.5	0.26	0.02
"	"	"	"	"	"	"	"	"						"			0.580	0.29	2.00	6.9	0.14	0.02
"	0.152	0.16	0.95	6.0	1.00	0.04											0.057	0.13	0.44	3.4	..	..
"	0.152	"	"	"	"	"											0.110 to	0.13 to		4.3 to	0.06 to	
0.71	0.145	0.17	0.85	5.0	0.94	0.068											0.089	0.16	0.68	5.2	0.08	
																	0.063	0.09	0.70	7.8	0.11	0.03
0.37	0.139	0.14	0.99	7.1	0.98		0.27 Cu										0.060	0.15	0.40	2.7	0.14	Trace
1½	0.138	0.22	0.63	2.9	0.21	0.029	0.07 Ni	60,000–62,200	36–37			104–114	5/16		450							
0.39	0.133	0.14	0.89	6.4	1.08	0.038	0.28 Cu										0.360	0.11	3.26	29.6	0.32	0.03
0.59	0.132	0.20	0.66	3.3	0.03	0.030	None	66,000			7.75						0.29	0.065	0.45	6.9	Trace	0.02
0.78	0.121	0.13	0.93	7.2	0.56	..	0.60 Cu															
1.57	0.094	0.13	0.72	5.5	0.13	0.033	None	62,500–64,000	25–29				0.16–0.39	Heavy Covered								
0.47	0.094	0.17	0.55	3.2	0.31	0.029	"	69,000	25.6	>180°	8.3	130					0.050	0.10	3.0 0.50	5.0	0.10	..
0.30	0.085	0.175	0.485	2.8	0.055	0.044	"	"				137	8 Gage	Coated		1						
0.39	0.066	0.18	0.37	2.1	0.05	0.04	0.38 Cu	61,000	33								0.055	0.11	0.50	4.6	0.05	0.02
"	"	"	"	"	"	"	"	"	"					Bare			0.065	0.09	0.72	8.0	"	"
0.83	0.053	0.097	0.55	5.7	0.03	0.032	None	51,500			7.57			"			0.029	0.065	0.45	6.9	Trace	0.02
0.39	0.045	0.11	0.41	3.7	0.05	0.07	0.10 Mo 0.26 Cu	61,000	28					Coated			0.055	0.11	0.50	4.6	0.05	0.02
0.39	"	"	"	"	"	"	"	"	"					Bare			0.065	0.09	0.72	8.0	"	"
0.39	0.040	0.10	0.40	4.0	0.00	0.03	0.15 Cu						0.16–0.24	Bare			0.036	0.08	0.45	5.6	..	0.029
"	"	"	"	"	"	"	"						"	"			0.162	0.10	1.62	16.2	0.19	0.019
"	"	"	"	"	"	"	"						"	"			0.238	0.10	2.38	23.8	0.23	0.019
																	0.252	0.08	3.15	39.0	0.64	0.023
1½	0.038	0.09	0.44	4.8	Trace	0.024	None	44,000–47,250	43–45			80–88	5/16		450							
0.47	0.052	0.11	0.47	4.2	0.12	0.018	None	57,000	28.2	>180°	17.0	91										
0.30	0.018	0.054	0.325		0.010	0.068	0.080 P					131	8 Gage	Coated		1						

Table 1 (B)—

BASE METAL												FILLER METAL						
Thickness, Inch	Mn × C	C	Mn	Mn/C	Si	S	Other	Tensile Strength, psi	Tensile Elongation, %	Bend Elong., ° or % of Angle	Impact Value	Brinell Hardness	Diameter, Inch	Mn × C	C	Mn	Mn/C	Si	S
0.047	<0.450	<0.2	2.25	11.3	..	..	None	103,000	20.4	>180°									
0.10	"	"	"	"			"	111,000	193	180°									
0.24	"	"	"	"			"	113,000	19.2	100°									
0.39	0.234	0.18	1.3	7.2	0.60	..	0.40 Cu	77,000–78,000			6.9–7.6								
"	0.222	0.33–0.37	0.6	1.7	0.3		None	89,000–91,000			6.1–7.4								
0.39	0.220	0.20	1.10	5.5	0.48	0.02	0.44 Cu 0.17 Cr	87,000	26					0.90	0.15	0.60	4.0	0.25	0.01
0.78	0.067	0.18	0.37	2.1	0.05	0.04	0.38 Cu	61,000	33					"	"	"	"	"	"
0.32	0.066	0.11	0.60	5.5	0.08	0.04	None							0.030	0.06	0.50	8.3	0.09	0.02
"	"	"	"	"	"	"	"							0.052	0.10	0.52	5.2	0.12	0.03
..	..	..	..	..	..	..	..							0.026	0.06	0.43	7.2	0.26	0.02
..	..	..	..	..	..	..	..							0.021	0.06	0.35	5.8	Trace	0.04
..	..	..	..	..	..	..	..							0.156	0.19	0.82	4.3	0.15	0.03
..	..	..	..	..	..	..	..							0.158	0.20	0.79	4.0	0.18	0.03
..	..	..	..	..	..	..	..							0.160	0.20	0.80	4.0	0.15	0.02
..	..	..	..	..	..	..	..							0.218	0.20	1.09	5.1	0.60	0.03
0.39	0.045	0.11	0.41	3.7	0.05	0.07	0.26 Cu	61,000	28					0.090	0.15	0.60	4.0	0.25	0.01
"	0.040	0.10	0.40	4.0	0.00	0.03	None						0.20–0.28	0.011	0.05	0.22	4.4	0.04	0.030
..	..	..	..	..	..	..	..						"	0.041	0.05	0.82	16.4	0.06	0.017
..	..	..	..	..	..	..	..						"	0.088	0.06	1.47	24.5	0.06	0.015
..	..	..	..	..	..	..	..						"	0.098	0.04	2.45	61	0.16	0.007
..	..	..	..	..	..	..	..						"	0.009	0.05	0.19	3.8	0.06	0.022
..	..	..	..	..	..	..	..						"	0.040	0.04	1.00	25	0.05	0.014
..	..	..	..	..	..	..	..						"	0.118	0.06	1.47	18.4	0.06	0.012
..	..	..	..	..	..	..	..						"	0.148	0.06	2.47	41	0.17	0.011

[1] Impact value, German standard round-notch specimen in mkg./cm.[2] Zeyen.[1]

[2] The elongation was determined at the first crack in a flush machined V weld with weld parallel to axis of tension, 1½ inch wide, 2½ inch parallel portion. Matting & Koch.[4]

[3] Cored electrodes with 2.7 Mn produced excessive slag. No nitride needles were observed. Kuchler.[7]

[4] The coating of the electrode contained 12% $CaCO_3$, 19% Ferro-Manganese (80% Mn), 3% Pyrolusite, 37% red Hematite, 6% Ferro-silicon-titanium, 23% sodium silicate. Weld metal contained 0.06 to 0.14 C, 0.44 to 0.85 Mn. Overheated water-quenched base metal had a hardness of 209 Brinell. Air-cooled overheated weld metal had a hardness of 220 Brinell. The fatigue limit (rotating cantilever, 30 × 10⁶ cycles criterion) is shown at the right.

	Fatigue Limit, psi	Tensile Strength, psi	Ratio of Fatigue Limit to Tensile Strength
Base Metal	41,000	86,000	0.481
Welded Joint	27,600	82,000	0.338
All-Weld-Metal	29,000	84,000	0.346
Overheated Base Metal	47,000	100,000	0.472
Musatti and Reggiori[8]			

[5] Microscopic cracks were observed originating at blow holes. It is not stated whether tensile specimens were machined. [6] Hanemann and Hilpert.[9]

Properties of Fusion Welded Joints in Plain, Low-Carbon Steel

Metal Arc Welds

ELECTRODE						JOINT										
Other	Tensile Strength, psi	Tensile Elongation, %	Bend Ductility, % or Angle	Impact Value	Brinell Hardness	Type	Procedure	Tensile Strength, psi	Tensile Elongation, %	Bend Ductility, % or Angle	Impact Value	Maximum Brinell Hardness	Base Metal C + Mn	Reference	Year	Footnote	REMARKS
None								103,500	10.7	90°			0.58	Zeyen[1]	1935		I. = 5.65√A for elong. %
Alloy								104,000	14.0	180°			"	"			
"								109,000	8.3	180°			0.52	"			
"								109,000	11.2	180°			"	"			
"								104,500	9.2	90°			"	"			
"								110,000	7.0	6.1°			"	"			
								87,500	..	44%			0.47	Orr[2]	1934		Unmachined tensile bars. Roller bend test
								82,000	..	46%			"	"			Heated ½ hr. 600° C. Furnace cool
								87,000	..	18%			"	"			
								80,400		46%			"	"			Heated ½ hr 600° C. Furnace cool
Alloy						V		90,000	..	39°		288	0.44	Sommer[3]	1931		Machined welds (0.39 inch). Bent 48°
"											7.7–15.2	163	0.39	Sikkema[4]	1937		Roller bend test. Charpy test in mkg./cm.[2]
None						V		54,000	..	37°			0.38	Hochheim[5]	1930		
0.08 Cu						V		56,000	..	28°			"	"			
0.04 Cu						V		59,000	..	23°			"	"			
	72,000	27.5		12.9				82,500	..	66°	8.0	209	0.47	Zeyen[1]	1935	1	Machined specimens. Elong. in 11.3√A
Alloy						V		88,000	..	39°			0.38	Sommer[3]	1931		
None						60° V		90,000					0.38	Matting & Koch[6]	1934	2	
0.04 Cu								80,000		80°			0.37	Hochheim[5]	1930		
								84,000–87,000		55 to 65%			0.36	Rapatz[12]	1935		Reduced section tension. DVM impact-mkg./cm.[2]
None						60° V		87,000	17				0.36	Matting & Koch[6]	1934	2	
"								70,000	10				"	" "			
"						V		71,500	5.5				"	" "		2	
												235	0.36	Kuchler[7]	1932	3	Elong. in 1.18 inches
None						"		73,000	6–7				"	"			
"				6.39		"		87,000	5				"	"			
													"	"			Impact Mesnager mkg./cm.[2]
Alloy						V		85,500				262	0.34	Sommer[3]	1931		
0.11 Cu						V		42,000		40°			0.33	Hochheim[5]	1930		
0.37 Cu						V		48,500		25°			"	"			
0.22 Cr													"	"			
0.35 Cu													"	"			
0.30 Mo						V		38,000		19°			"	"			
None	83,000	15.8	52	7.5	165	V		82,000	12.0	30%	5.9	240	0.35	Musatti & Reggiori[8]	1934	4	
Alloy						V		82,000					0.34	Sommer[3]	1931		
	72,000	27.5		12.9				83,500		91°	8.9	190	0.38	Zeyen[1]	1935	1	
"						75° V		62,300					0.31	Hanemann & Hilpert[9]	1932	5	
"						V		68,000	6.6				0.31	Kuchler[7]	1932	3	Elong. in 1.18 inches
"						"		68,000	5–8.3				"	"			
"				6.39		"		75,000	10–11				"	"			Impact Mesnager, mkg./cm.[2]
						V		53,000		30°			0.30	Hochheim[5]	1930		
						"		67,500		28°			"	"			
0.04 Cu						X		60,500		30°			0.29	"			
0.24 Mo						"		52,500		25°			0.28	"			
0.05 Cu						V		58,350–60,000	32.5–37.5			121	0.31	Hopkins[10]	1934	6	Elong. in 2 inches
0.13 Cu						X		69,000					0.27	Hochheim[5]	1930		
						75° V		61,800			1.80		0.29	Hanemann & Hilpert[9]	1932		Charpy mkg./cm.[2]
						X		61,500		21°			0.26	Hochheim[5]	1930		
						X		81,000	15		11.0		0.23	Stiller[11]	1934	7	Elong. % in 0.39 inch
"	72,000	27.5						76,000		>180°	10.4	152	0.25	Zeyen[1]	1935	1	
												163	0.25	Roberts[13]	1935		No Martensite
None						60° V		64,000	15			197	0.23	Matting & Koch[6]	1934	2	
"						"		63,000	8		1.37		"	" "		2	
						75° X		52,500					0.18	Hanemann[9] & Hilpert	1932		Charpy mkg./cm.[2]
None						60° V		60,000	17				0.17	Matting & Koch[6]	1934	2	
"						"		59,000	11				"	" "		2	
"						60° V		53,000		9%	1.0		0.16	Becker[14]	1935	8	
"						"		51,000		11%	2.5		"	"			
"						"		50,000		5%	1.5		"	"			
"						"		48,500		4%	1.0		"	"			
												114	0.15	Hopkins[10]	1934	6	Elong. in 2 inches
						V		52,800–55,400	34–38		12.4	142	0.18	Zeyen[1]	1935	1	Machined specimens
								73,000		>180		153	0.10	Roberts[13]	1935		Elong. in 11.3√A No Martensite

Oxyacetylene Welds

FILLER METAL						WELDED JOINT										
Other	Tensile Strength, psi	Tensile Elongation, %	Bend Elongation % or Angle	Impact Value	Brinell Hardness	V or X	Procedure	Tensile Strength, psi	Tensile Elongation, %	Bend Elongation % or Angle	Impact Value	Maximum Brinell Hardness	Mn Base Metal C + ½	Reference	Year	Footnote	REMARKS
								103,000	10.0	180°			0.52	Zeyen[1]	1935	1	
								102,000	7.8	180°			"	"			
								100,000	8.0	95°			"	"			
								82,500–91,000		80–90°	7.4–8.3		0.37	Holler & Frankenbusch[15]	1935		DVMR impact-mkg./cm.[2] Reduced section tensile
								90,500–91,500		180°	6.3–7.3						
None						60° V		72,500	21				0.42–	" "			DVMR impact-mkg./cm.[2] Reduced section tensile
"						"		57,000	17				0.46				
None						60°	Right-hand	64,000		48%			0.36	Matting & Koch[6]	1934	2	
"						"	"	63,000		55%			0.23	" "			
"						"	"	63,000		48%			0.20	Streb & Kemper[16]	1933		
"						"	"	60,000		48%			"	" "			
"						"	"	64,000		51%			"	" "			
"						"	"	64,000		44%			"	" "			
"						"	"	63,000		48%			"	" "			
"						"	"	64,000		27%			"	" "			
None						60° V		58,000	25				"	" "			
None						60° V		51,000					"	" "			
"						"		50,000					0.17	Matting & Koch[6]	1934	2	
"						"		44,000		6%	3		0.16	Becker[14]	1935		Analysis is of deposited metal
"						"		42,500		4%	3.5		"	"			
"						"		55,000		32%	2		"	"			
"						"		56,000		20%	1		"	"			
"						"		53,000		9%	8.5		"	"			Atomic Hydrogen
"						"		54,000		12%	5.5		"	"			"
											2		"	"			"
											2		"	"			"

[1] Weld metal contained 0.10 C, 0.60 Mn, 0.20 Si, average. All specimens were annealed for 2, 6, 12 or 36 hrs. at 1200° F. Charpy impact value of plate fell to less than 5 ft.-lb. at −25° F., but Charpy value of weld was over 15 ft.-lb. at −50° F.

[2] Impact value in mkg./cm.²; specimen had 0.16 inch diameter notch, area of fracture was 0.33 sq. in. The physical properties after annealing (no details) were:

	Tensile Strength, psi	Elongation % in 0.39 Inch	Notch Impact Value, Mkg./Cm.²
Annealed 650° C.	72,500	20	14.8
850° C.	71,500	50	20.8
920° C.	71,000	39	19.3

[3] Tensile specimen was reduced-section, 0.79 inch wide, 0.79 inch radius. Roller bend specimen was 1.17 inch wide, 0.39 inch thick, roller spacing 1.97 inches, plunger 0.79 inch thick. Charpy impact specimen (mkg./cm.²). The forgeability of the high-manganese welds was best; the low-manganese weld was worst. The carbon content of the weld was 0.03 to 0.04%; the manganese contents of the four welds were 0.30, 0.82, 1.75 and 1.94 Mn, respectively.

Rotating-bend fatigue strength of machined specimens was also determined. Becker.[14]

The plates were 3 x 9 inches, the bead being deposited by an AC automatic machine in the middle of the 3 inch width. The welding speeds for plates $^1/_2$ inch thick were 12 and 18 inches a minute; for plates 1 inch thick, 8 inches a minute.

The hardness measurements were plotted in four ways: 1. Against % C, 2. Against % C plus % Mn, 3. Against % C plus $\frac{\%Mn}{5}$, 4. Against % C plus $\frac{\%Mn}{7}$. The result of the third and fourth plots was approximately a straight line with slight convexity to the composition axis and irregularities in the higher ranges of carbon and manganese content. The other two plots were much less nearly straight lines. Therefore, the hardening power for manganese may apparently be given as $^1/_6$ to $^1/_7$ that of carbon. Further work was considered essential before a final solution of the carbon-manganese question could be reached. For example, the significance of a straight line in the composition plots has not been definitely settled.

A comparison of the results of the hardness investigations on surface beads with the investigations of welded joints clearly reveals the vulnerable points of each. The physical properties of a welded joint are not directly related to the maximum hardness. On the other hand, the "weld quench" obtained in a single bead appears to be a significant indication of the limitations which must be placed on welding variables, such as plate thickness and welding speed, in order to avoid dangerously high hardness. In multi-layer welding, particularly if the beads are not allowed to cool between runs, the weld quench, in the usual sense of the term, is absent.

Meunier and Rosenthal made a study of the bend, notch-impact and hardness value of beads deposited on the steels shown in Table 5. The steels containing chromium and molybdenum, strictly speaking, do not enter the present discussion. The dimensions of the specimens are not stated but the thickness appears to have been 0.59 inch. The welding speed was 7 inches a minute; current was 180 amps. (no details). The hardness was measured on a Rockwell tester. The bend ductility was measured on a specimen 0.32 to 0.39 inch thick from which the fused material had been machined. The plunger method was used, the heat-affected zone being in tension. Notch impact value was determined on a Mesnager specimen, the root of the notch (0.08 inch deep, 0.08 diameter) being at the junction of fused metal with heat affected zone, Fig. 1.

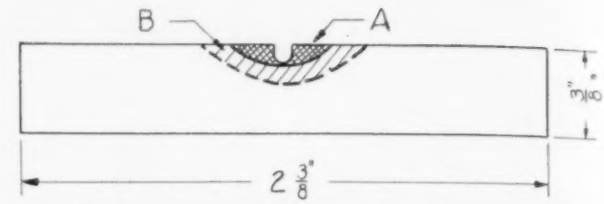

Fig. 1—Notch Impact Specimen ($^3/_8$ Inch Square) Used by Meunier and Rosenthal.[29] The notch Is 0.04 Inch Radius, 0.08 Inch Deep and Its Root Is at the Base of the Fusion Zone of a Specimen with Bead Deposited on Surface.

A = fusion zone B = heat-affected zone

It is unfortunate that the specific effects of manganese are inextricably confused by the effects of carbon in the steels. That is, the unalloyed steels containing high manganese also contained high carbon. The results on the whole suggest that manganese, weight for weight, is far less effective than carbon in promoting hardness and lack of ductility. It was also found for 0.44 C steel that the ratio: maximum hardness in vicinity of bead to hardness of base metal rose from 1.3 at $1^3/_4$ inches a minute to 2.6 at 7 inches a minute, both at 180 amps. but fell from 2.65 at 100 amps. to 1.5 at 400 amps., both at 7 inches a minute. The plunger bend specimens cracked at the junction between base metal and heat-affected zone. The heat-affected zones of the steels containing 0.33 and 0.44 C contained large amounts of martensite.

The deposition of more than one bead has a salient effect in decreasing brittleness and maximum hardness. Additional results on the effect of three layers of an electrode containing 0.15 C, 0.5 Mn, 0.02 Si on a steel containing 0.244 C, 1.1 Mn, 0.32 Si are given in Table 6. The decrease in hardness effected by multi-layers was rather small but the refinement of grain structure was pronounced.

Table 5—Mechanical Properties of Steels with a Bead of Weld Metal Deposited on the Surface. Meunier and Rosenthal[29]

	$C + \frac{Mn}{7}$	0.02	0.19	0.39	0.45	0.67	0.33	0.39	0.37
	Carbon	0.02	0.10	0.256	0.33	0.44	0.142	0.195	0.219
	Manganese	(Armco)	0.605	0.906	0.861	1.16	1.32	1.33	1.03
	Silicon		0.1	0.066	0.19	0.1	0.33	0.58	0.34
Analysis	Sulphur	≤0.2			0.02				
	Phosphorus		≤0.06	≤0.06	0.023	≤0.06			
	Chromium								0.27
	Copper						0.36	0.31	0.55
	Molybdenum							0.10	
Mechanical Properties of Base Metal	Tensile Strength		57,000	74,500	87,500	104,000		74,000	
	Yield Strength, psi		39,600	46,400	55,000	64,000		51,000	
	Elong. 7*d* %		25	22.8	23.5	18		20	
Bend Angle Degrees	Unwelded		180	180	180	180	180	180	180
	1 Layer		180	180	110	8.5	135	90	90
	4 Layers		180	180	150	73	180	180	180
Bend Elongation %	Unwelded		45	45	45		45	45	45
	1 Layer		34	28	25	5	23	19	19
	4 Layers		39	40	35	27	32	30	33
Notch-Impact Value Mesnager Kgm./Cm.^{2}	Unwelded		>25	11	10.2	11.2	10	10	10
	1 Layer		16	9.5	5.2	0	10	8.3	8.7
	4 Layers		16.2	12.3	10.1	10.7	10	10.7	10.5
Maximum Brinell Hardness	Unwelded	107	130	150	182	200	165	176	180
	1 Layer	130	152	197	270	530	235	260	260
	4 Layers	113	144	167	192	240	190	200	195

Table 6—Maximum Hardness in Surfaced Steels. Meunier and Rosenthal[29]

Current Amps.	Speed of Advance of Electrode, In./Min.	Brinell Hardness 1 bead	Brinell Hardness 3 beads	Remarks
180	7	175	160	.....
350	7	155	150	.....
350	3	150	150	.....
180	7	152	140	Preheated to 600° C.

Investigations of the effect of carbon and manganese on the hardness of deposits intended to increase the wear resistance of the base metal have been made by Dahl and Sandelowsky,[30] Salsich,[31] and McKinney.[32] In view of the absence of a criterion of dangerous hardness, their results are not particularly informative. The maximum Brinell hardness in sections of mild steel (97–120 Brinell) with one or two surface beads has been determined by Dahl and Sandelowsky, Table 7. The electrodes were 0.16 inch diameter.

McKinney[32] determined the hardness and studied the microstructure of cast steels (0.76 C, 0.77 Mn, 2.09 Si and 0.65 C, 0.78 Mn, 2.05 Si) in the form of cones $^3/_4$ inch high with beads of bare electrodes deposited on the sides. The weld metal deposited by the electrode contained 0.075 C, 1.17 Mn, 0.27 Si. The Brinell hardness of the deposit on the high-carbon steel (332–351 Brinell) was 174 to 192 which was increased to 269 by 200 drop-hammer blows. On the lower carbon steel (321–340 Brinell) the weld metal had a hardness of 156 to 207 Brinell. The microstructure of the welded cones appeared to contain considerable proportions of troostite or martensite. Salsich's investigation[31] was similar to, but less extensive than McKinney's.

General Observations

The following summary of the more important contributions to the large body of brief observations and utterances of opinion on the effect of carbon and manganese in welding plain-carbon steel is perhaps more favorable to manganese than might have been expected. Certainly not all of the possible disadvantageous effects of high manganese content in plain-carbon steels have been discussed in the literature.

According to Granjon[33] and the French Institute of Welding,[34] manganese increases the weldability (absence of injury to the mechanical properties on welding). A steel containing 0.15–0.20 C, 1–1.50 Mn and 0.15–0.25 Si has as good weldability as lower strength steels. Manganese increases the tensile strength and decreases the grain size in the weld. The harmful effect of an increase in carbon in the range 0.10 to 0.15 C can be corrected by increasing the manganese or silicon. Houdremont[35] strongly recommends steel containing 3 to 5% Mn, 0.12% C for welded constructions on account of its insensitivity to welding cracks.

There appears to have been no prejudice among early welding investigators for or against manganese. Diegel[36] in 1922, for example, made satisfactory oxyacetylene welds in steel containing 0.14 C, 1.08 Mn. The characteristic properties of the medium-manganese steels had not been adequately determined at that time and the steels appear to have been little used even apart from welding on account of difficulties in fabrication, riveting, etc. The tendency for some medium-manganese steels (0.15 C, 1.25 Mn) to display laminations and woody fracture is cited by Bureau of Construction and Repair, Navy Department (private communication, August 1937) as a cause of the poor behavior of T weld bend specimens in these steels. Hahn[37] found that the forge weldability (ability to effect union) of steel was not decreased by 3.4 Mn with 0.34 C.

Johnson[38] had no difficulty in gas welding tubing and sheet containing 0.24 C, 0.52 Mn, (tensile strength 60,000 psi, size not stated) with a rod containing 0.07 C. Taylor,[39] however, states that gas welds in thin tubing containing 0.3 C, 1.75 Mn are likely to be brittle. He has determined the strength of spot welds in single and double shear in 10 and 18 gage steel of this composition. A steel containing 0.42 C, 1.1 Mn, Zeyen[1] found, failed in the Fokker test (bead of oxyacetylene weld metal deposited from edge to center of a square sheet 0.047 inch thick) whereas a steel containing less than 0.2 C, and 2.25 Mn did not. According to Paterson,[40] welding cracks bear no relation to manganese content up to 0.55%. On the contrary, Swinden[41] states that there appears to be greater difficulty in avoiding welding cracks if the manganese content is between 0.3 and 0.6% than with higher or lower manganese content. Hughes[42] found that difficulty is encountered in spot welding $^1/_2$ inch plate if the sum of carbon and manganese is greater than 0.80%. A micrographic study was made of a resistance butt weld in steel containing 0.50 C, 0.53 Mn, $^1/_2$ inch thick. The maximum hardness was Rockwell B 98. A good micrographic study of arc-welded Mn-V steels (0.2 V) has been made by Harris.[43] It should be noted that manganese-vanadium steels ordinarily contain approximately 0.10% V. Such steels in the form of non-stress relieved all-weld-metal (0.13–0.18 C, 1.20–1.45 Mn, 0.15–0.30 Si, 0.08–0.12 V, coated electrodes) develop 68,500 psi yield strength, 87,800 psi tensile strength, 18.5% elongation in 2 inches, 42% reduction of area, and 49 ft.-lb. Izod, according to A. W. Demmler (private communication, August 1937).

According to Leitner,[44] who investigated bare electrode welds in five steels containing 0.1 C, 0.1 Si and 0.32 to 1.73 Mn, manganese in base metal decreases the porosity of welds.

Manganese does not appear to be an important factor in nitrogen pick-up. Haardt[45] deposited bare and coated electrodes, 0.16 inch diameter, containing 0.07 C, 0.4 Mn, 0.01 Si on a plate containing 0.1 C, 3.5 Mn, 0.2 Si, 251 to 260 Brinell. The maximum hardness, 265 Brinell, occurred in the heat-affected zone 0.02 inch from the line of fusion. It was found that less manganese was picked up by the weld metal than silicon in a steel of the same silcon content because manganese is more rapidly oxidized and slagged than silicon. Nitrogen pick-up was less with the manganese steel than with nickel or copper steels with similar alloy content, but greater than with silicon steel. Séférian[46] found that manganese was not a factor in nitrogen pick-up in steels containing 0.03 to

Table 7—Maximum Hardness in Surfaced Mild Steel. Dahl and Sandelowsky[30]

Type of Electrode	Composition of Electrode, % C	Si	Mn	C + Mn	After Deposition, % C	Si	Mn	C + Mn	Maximum Brinell 1 Bead	2 Beads
Bare	0.08	0.00	0.46	0.15	0.01	trace	0.30	0.05	159	127
Covered	0.11	0.13	0.70	0.21	0.02	0.02	0.50	0.09	155	138
Coated	0.57	0.08	0.44	0.63	0.14	trace	0.30	0.18	205	202

Table 8—Effect of Manganese on the Critical Cooling Velocity. Jellinghaus[55]

Manganese Content %	Critical Cooling Velocity, Degrees Centigrade per Second With 0.3% C	With 0.8% C
0.0	750	490
0.3	about 550	430
0.5–0.6	250	160
1.0	150	70
1.5	60	20

0.30 C, 0.01–0.4 Mn. On the other hand, the British Engine Boiler & Electrical Insurance Co.[47] state that manganese is a powerful inhibitor of nitride needles.

Steel for flame-hardened brake drums should not contain over 0.7 Mn, according to Kleiner.[48] Drums with 0.425 to 0.51 C, 0.93 to 1.39 Mn had a strong tendency to cracking during flame hardening. Drums with 0.2–0.33 C, 0.55–0.69 Mn gave no trouble. Shorter[49] mentions that manganese is an element that will replace carbon from the standpoint of flame hardening. According to Greger,[50] flame cutting is applicable to steels up to at least 1.3% Mn without difficulty. According to A. W. Demmler (private communication, August 1937) manganese-vanadium steel (0.17 C, 1.35 Mn, 0.10 V) has been flame cut without difficulty in thicknesses up to 4 inches. Welding with medium-manganese filler rod is often recommended. The well-known high-test rod for oxyacetylene welding contains 0.15–0.20 C, 0.30–0.40 Si, 0.80–1.0 Mn. Fry[20] found that 1% Si structural steel is best welded with medium-manganese electrodes and a writer in 1919[51] mentioned that bare electrodes, 0.148 in. diameter, containing 0.39 C, 1.01 Mn, 0.12 Si deposited high-quality weld metal (76,000 psi tensile strength, 7.5% elongation, 13% reduction of area).

The Effects of Manganese in Steel

The general effects of manganese in plain-carbon steel have been dealt with in a number of articles and books, notably by Fulton.[52] A complete review of the manganese problem, of course, is out of the question in the present report. Instead, a summary is given in this section of the physical effects of manganese in plain-carbon steel (up to 0.4 C) that may be important in welding.

1. Austenite Transformation

Manganese (up to 4%) has no effect on the *nature* of the austenite transformation; that is, as the cooling rate from above Ac3 to room temperature is increased, austenite transforms to finer and finer pearlite, and finally to martensite.

Manganese is one of the most effective elements, weight for weight, in decreasing the critical cooling velocity required to suppress the reaction: austenite→ferrite and carbide on cooling. Bain[53] found that in 0.55% C steels quenched from above A3 to 315° C., the time for 50% of the austenite to transform was 30 seconds with 0.5 Mn and 8 hours with 3.8 Mn. In steels containing 0.6 C and 2% each of common alloying elements, Döpfer and Wiester[54] found that the time of incubation of the gamma alpha reaction at 225 to 500° C. was greatest with 2% Mn and that the maximum reaction velocity was least. The effect of manganese on the lowest cooling velocity from above A3 to room temperature that will produce martensite is given by Jellinghaus[55] in Table 8. Somewhat similar results were secured by Esser and coworkers,[56] Table 9. Again manganese was found to be most effective element in lowering the critical cooling velocity.

Table 9—Effect of Manganese on Critical Cooling Velocities. Esser, Eilender and Majert[56]

C	Mn	Quenching Temperature, Degrees C.	Critical Cooling Velocity Degrees C. per Second A	B
0.42	0.55	950	120	550
0.40	1.60	950	26	50
0.35	2.20	950	2	8

A = cooling velocity necessary to obtain a slight detectable amount of martensite in the microstructure.
B = the lowest cooling velocity that will entirely suppress ferrite and secure 100% martensite.

Manganese appears to act both in solid solution (increased depth of hardening) and in carbides (decreased depth of hardening). In the lower-carbon steels the former effect predominates. An interesting example of the effect of manganese is provided by Maurer who showed that a steel containing 2% C, 2% Mn could be quenched completely austenitic. In all steels up to 0.4 C and 4 Mn the most drastic quench has not been able to prevent partial decomposition of austenite.

2. Grain Size

Experience has shown that manganese (up to 4%) increases the grain coarsening tendency in tool steels, but decreases it in case carburizing steels. According to Leihener,[57] fine grained steels, 1 inch diameter, containing 0.26 C, 0.95 Mn, give practically no cracks on hardening, whereas coarse-grained steels of the same analysis give trouble due to hardening cracks owing to excessive martensite reaction. The effect of a small addition of aluminum is said to outweigh the effect of manganese and coarse grain. Medium grain steels (3 to 5) are said to give least difficulty because finer grained steels are subject to flaking. The conclusion is that manganese in solid solution has little effect on grain coarsening tendency apart from the carbide question. In the coarse-grained, overheated zone of a weld manganese is in solid solution and promotes deep hardening. In finer-grained zones, manganese may be partly in the carbide form and may thus promote shallow hardening, although it appears that the effect of manganese carbide on grain size is rarely observed. The subject of grain size remains controversial.

3. Critical Points

Abbott states that 0.3443° C. must be deducted from the Ac3 of pure iron (908° C.) for each 0.01% Mn, and 2.237° C. for each 0.01% C. Abbott's determinations appear to have been made on heating in salt baths.

Bain, Davenport and Waring,[58] using alloys of commercial purity and a method of critical point determination corresponding to an infinitely slow cooling rate, found that the equilibrium range of the austenite plus ferrite field is approximately 600° to 740° C. at 4% Mn, 0.2 to 0.4 C. Up to about 10% Mn the only phases present in the Fe–Mn–C system are the alpha, gamma and carbide phases. During cooling the alpha phase separating is low in manganese so that the remaining gamma is enriched in manganese and the transformation is retarded. Increasing the carbon content with constant manganese tends to raise the critical range, although evidence to the contrary is also available. The equilibrium diagram of the Fe–Mn–C system is by no means settled (Bain and coworkers, Walters and coworkers, Linden, Vogel). The nature of the carbide and the carbide solubility are major points of variance among the investigators. The epsilon phase does not appear to be a factor up to 0.4 C, 4 Mn. The dilatometric investigation by Merz[59] appears to show that the expansion during

cooling through the critical range is not materially increased by 2.6% Mn in steels containing 0.35 or 0.7% C.

4. According to Bain and coworkers,[58] the eutectoid is displaced to the left 0.08% C for each 1% Mn up to 3.8% Mn. Reed[60] found that the displacement to the left is 0.17% C for each 1% Mn up to 5% Mn. Houdremont[35] arrived at a value of 0.06% C per 1% Mn up to 3% Mn. Only Bain and coworkers appear to have taken the effect of cooling rate into account. Their values are most reliable, but represent equilibrium conditions approximately corresponding to extremely slow cooling. The large displacement of the eutectoid composition toward lower carbon contents as the manganese content is increased implies that medium-manganese steels have a greater pearlite content than plain-carbon steels of the same carbon content. Larsen[61] has reported an unusual cellular distribution of pearlite in cast medium-carbon, medium-manganese steel.

5. Manganese is considered to retard the rate of spheroidization of carbides.

6. The effect of manganese on the thermal constants is insignificant up to 3% Mn, disregarding the influence of manganese on critical quenching speeds. The electric resistance of iron at 20° C. is tripled by the addition of 4% Mn.

7. Manganese forms oxide (MnO), sulphide (MnS) and silicates in steels under appropriate conditions. Careful refining practice is often considered necessary in medium-manganese steels to avoid strings of inclusions that seriously lower the transverse notch impact toughness.

8. Mechanical Properties

The physical properties of heat-treated medium-manganese steels, e.g. SAE 1330–1340, series T and X, are quite well known. Fulton[52] characterizes the properties of these steels in the properly treated condition as excellent. Houdremont[35] has shown that steels containing 0.12–0.15 C, 3.13 to 5.20 Mn, 0.4 Si have remarkably good strength and toughness when properly heat-treated (900° C. oil quench, 600–650° C. draw). The results of tensile notch impact and dilatometer tests by Hamasumi[62] on 25 samples of steels containing 0.4 to 8% Mn, 0.1 to 0.4% C, while not so encouraging as Houdremont's results, yet show that good properties may be expected at least up to 3% Mn. Bardenheuer and Schitkowski,[63] however, found that in a series of forged and cast steels containing 0.13 to 0.44 C with 0.7 to 2.8 Mn tested at −20 to +100° C., the toughness decreased to a pronounced extent with increase in Mn.

The Charpy impact value of medium-manganese steels at −50 to +100° C. has been determined by Hiemke and Schulte,[64] Table 10. The rate of cooling for determining critical points was not stated. The steel "C" had not entered the brittle transition zone even at −50° C. At room temperature the impact value increased with manganese content.

Table 10—Notch Impact Value of Normalized Medium-Manganese Steels. Hiemke and Schulte[64]

	Chemical Composition, %			Critical Points Degrees Centigrade		
	C	Mn	Si	Ac1	Ac3	Charpy Value
A	0.25	0.49	0.14	740	840	4 ft.-lb. at −20° C.
B	0.25	1.24	0.06	725	830	16 ft.-lb. at −40° C. but 3 ft.-lb. at −50° C.
C	0.26	1.32	0.44	730	840	29 ft.-lb. at −50° C.

Short-time tensile tests on medium-manganese steel sheet, 0.04 inch thick annealed for 1/2 hr. at 650° C. and air cooled are reported by Bollenrath and Cornelius[65] in Table 11. The strips were 0.79 inch wide and were rapidly heated to test temperature. They were pulled at the rate of 0.04 inch a minute. The reduction of ductility above 800° C. was independent of previous treatment, such as cold rolling or heat treatment.

Tensile and Izod tests by Strauss[66] on a large number of medium-manganese basic open-hearth and electric steels containing 0.32 to 0.51 C, 0.04 to 0.31 Si, and 1.05 to 1.88 Mn showed that the deterioration in properties due to overheating 110° C. above the usual quenching temperature was no greater than in other alloy steels (1 to 3% Ni or 1% Cr, with or without 2 to 3% Ni) of the same carbon content. Quenching below Ar3 or between Ac3 and Ar3 did not adversely affect the toughness. Strauss[66] concluded that in small sections (1 inch diameter) the medium-manganese steels compared favorably with other structural alloy steels. In the higher ranges of strength and hardness (200,000 psi tensile strength and over) the notch impact resistance was lower than for some other alloy steels. The steels were subject to temper brittleness.

A large number of other investigators have studied the mechanical properties of medium-manganese steels but those cited in the present summary appear to have established the essential facts that may be of interest to the welding industry.

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Table 11—Short Time, High-Temperature Tensile Tests. Bollenrath and Cornelius[65]

			Tensile Strength, psi					Elongation, % in 4 inches				
C	Mn	Si	20° C.	400	600	800	1,000	20	400	600	800	1000° C.
0.12	1.42	0.23	67,000	68,000	28,500	7,000	4,000	22	17	28	64	20
0.13	2.26	0.53	100,000	70,000	33,000	7,000	4,000	20	19	28	52	15
0.56	0.53	0.01	83,000	72,000	28,000	7,000	4,000	20	18	30	15	17

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(*Continued from page 3*)

Silicon	0.15–0.25 (No other deoxidizers permitted)
Sulphur	0.025–0.040
Phosphorus	0.040 maximum
Total other elements	0.50 maximum (with no single element more than 0.10 per cent)

Plates shall be $^1/_2$ inch, 1 inch and $1^1/_2$ inch in thickness.

Group 1.—Chemical and physical tests of the base metal and photomicrographs of plates representing each composition.

Group 2.—Depositing weld beads and making hardness surveys on the cross-sections of these samples. Photomicrographs are to be included as well as the effect of temperature on the base metal prior to welding.

Group 3.—Rigid frame weld tests. This will include the welding and preparation of test plates. The hardness and tension impact tests are to be made on the welded joint.

Group 4.—Physical properties of the welded joint to be determined by making welds in the full thickness of the plate and subjecting them to reduced tensile, free bend, radiographic, microscopic and tension impact tests.

The total estimated cost of the project for materials, welding and testing is approximately $10,000.

Battelle Memorial Institute

Mr. Clyde E. Williams, Director, Battelle Memorial Institute, Columbus, Ohio, has announced the appointment of Dr. A. U. Seybolt to the technical staff. Dr. Seybolt, who received the Ph D. degree from Yale in 1936 has been assigned to the Division of Non-Ferrous Metallurgy, where he will be engaged in work on copper-base and lead-base alloys.

Welded Piping Systems

The advantages of welded joints in installing piping systems are discussed in "Welded Piping," a 12-page, illustrated booklet, published by The Linde Air Products Company, New York.

Subjects of particular interest to builders, architects and engineers are treated in sections on: pipe and services to be welded; lighter than standard weight pipe; fast, economical welding methods; layout, drawings and specifications; welded joints and fittings; plan of construction; shop and field fabrication, and piping rection.

Man in a Chemical World

By A. CRESSEY MORRISON

The preparation and publication of this volume was authorized as a result of the great success of the celebration of the Three Hundredth Anniversary of the founding of the chemical industry in the United States of America, in 1635, by John Winthrop, the younger, then Governor of Connecticut.

This book is designed to place before the public, in simple language, the unsurpassed contribution of the chemical industry not only to the well-being of the individual, but its unparalleled place as a contributor to human comfort, protection, the advancement of civilization and to the upbuilding of the United States of America.

THE ENGINEERING FOUNDATION
WELDING RESEARCH COMMITTEE

Sponsored by American Welding Society and American Institute of Electrical Engineers

Supplement to the Journal of the American Welding Society, October 1937

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Translation

A very limited number of copies of a translation of a part of a German article entitled: "Effects of Shrinkage Stresses on the Fatigue Strength of Welds" are available for loan. The original article was written by Dr. F. Kaufmann, of the Technical College, Darmstadt, Germany, and was published in *Technische Mitteilungen Krupp*, **5**, June 1937. The section of the article that was translated describes the construction, operation and calibration of a 30 ton (metric) Losenhausen Pulsator, and the calibration of a 25 ton (metric) Amsler Pulsator.

Bureau of Standards New Housing Program

The National Bureau of Standards has received a new appropriation from Congress to be used for the technical study of low-cost housing. In connection with this study, the Bureau has recently issued Letter Circular LC-502 and a supplement, LC-502A, copies of which can be obtained from its office in Washington. LC-502 outlines the objectives, procedure and scope of the Bureau's new research program on building materials and structures. LC-502A, being supplementary, deals only with the structural properties of low-cost house construction, giving detailed information on the various loading tests to which the walls, non-bearing partitions, floors and roof are to be subjected.

Participation of any interested industrial groups in the program is invited, for the purpose of cooperating by designing, constructing and supplying specimens upon which tests can be made. The results of the tests will be made available to the public, if the Bureau so desires. There will be no charge for testing. This testing program is under the general supervision of H. L. Whittemore one of the prominent members of the AMERICAN WELDING SOCIETY.

Among other things, the program will include the testing of elements of a house (such as roof, walls and floors) suitable for single houses, row houses and low-cost apartment houses. These tests will be made on a representative portion of the element. New systems of construction need not necessarily use new materials, but merely have a change in design, dimensions, method of fabrication or workmanship of an old material. Conventional systems of construction will be studied only to the extent of forming a proper basis of comparison for new systems of construction.

There are certain limitations as to cost, which confine the program to low-cost individual housing, including low-cost apartment house construction, but these limitations are fairly liberal.

In view of the fact that this work is just starting and about a dozen companies have already indicated that they will submit specimens, of which many use steel fabricated by welding, this program is good news for the welding industry.

ENGINEERING FOUNDATION—WELDING RESEARCH COMMITTEE—Sept. 30

Welding Research Activities

The end of the fiscal year 1936–37 finds the Welding Research Committee well established. The work has been divided into three divisions, namely, Fundamental Research, Industrial Research and Literature. Each division has to its credit many important accomplishments. A total of 32 reports comprising 368 printed pages, were published during the year. The reports dealing with critical digests of the literature are first mimeographed and distributed to experts all over the world for criticism before publication. The Welding Research Committee and its three divisions have drawn into the work the leading welding research experts in this country.

The needs of the Committee are growing apace. Additional library research workers are needed. Provision must be made for grants-in-aid to selected universities, and for the establishment of full and part-time research fellowships, and for the support of specific researches. Several important large projects are under consideration which will require independent financing.

Through a unique cooperative arrangement a service has been established whereby translations of the most important articles on welding published in the various technical periodicals of the world are made available to research workers and others interested. Personal visits have been made to a number of universities interested in fundamental research with the idea of obtaining first-hand knowledge of available facilities and talent for researches to be undertaken under the auspices of the Fundamental Research and Industrial Research Divisions. The important welding problems needing solution have been compiled and are available to those interested.

The Committee is cooperating with a number of societies, such as the A. S. T. M. the A. S. M. E. Boiler Code Committee, AMERICAN WELDING SOCIETY, and Governmental departments, in all matters relating to welding research. Contacts have been established with important groups in England, France and Germany. Joint meetings have been held with the A. S. T. M. and the A. W. S. A full day and one-half of the Annual Meeting Program of the AMERICAN WELDING SOCIETY will be gven over to the reports of the Fundamental and Industial Research Divisions.

Effect of Welded Top Angles on Beam-Column Connections*

By INGE LYSE† and GLENN J. GIBSON‡

Introduction

THIS paper presents further studies of the top angle beam connections designed for end restraint as described in the October 1936 issue of THE WELDING JOURNAL. The investigation was sponsored by the Structural Steel Welding Committee of the AMERICAN WELDING SOCIETY and was carried out at the Fritz Engineering Laboratory of Lehigh University. The first investigation showed that a beam connection using top angles to provide the end restraint and seat angles to carry the vertical load produced desirable conditions. This method of construction seemed to be the most economical type in regard to both materials and welding practice. The advantages of welded beam connections are rigidity under ordinary working conditions and at the same time sufficient flexibility to allow the end of an overloaded beam to rotate enough to insure flexural failure of the beam at its center before failure occurs at the end connection.

The initial yielding of the top angles under working conditions needed further investigation to provide confidence in the connection. This question led to the study of sustained and repeated loading in both the cantilever and complete beam tests. Since only twelve-inch beams were investigated in the first report, eight- and sixteen-inch beams with various spans and different size top angles were tested to check the general application of the analysis and the theory of design which were presented in the previous report. To complete the design of the connection, tests were made to determine the reaction strength of beams supported on seat angles when the beam was rigidly welded to the seat. From the results of these tests a theory was developed to determine the range of sizes and spans of beams that could economically be connected by top angles providing fifty per cent end restraint.

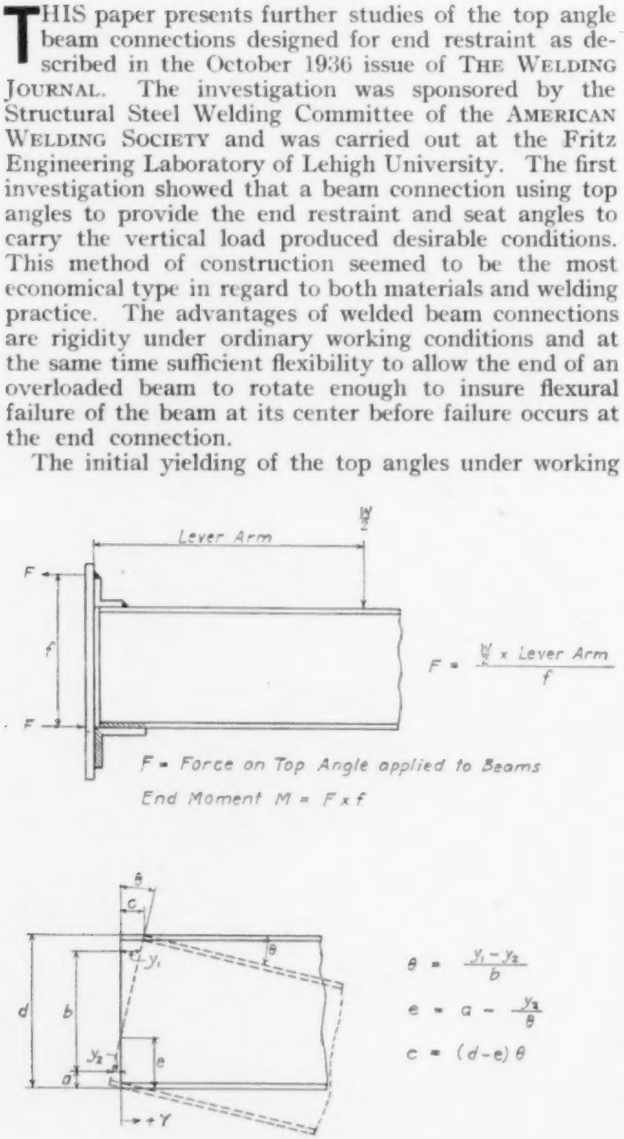

Fig. 1—Method of Analysis of Cantilever Connections

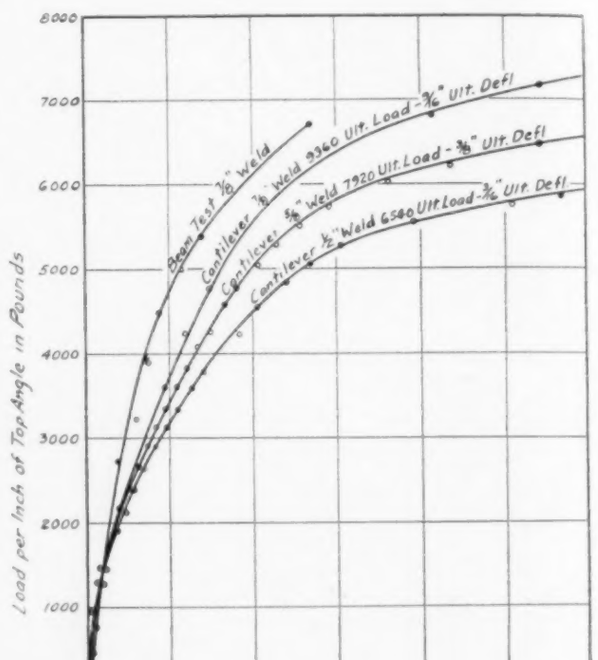

Fig. 2—Effect of Size of Weld on the Strength of a 7/16-Inch Top Angle

Analysis of Connections

The analysis of the top angle connection which was presented in the previous report is recorded below. Figure 1 shows the location of the welds and the method of computing the loads and forces acting on the members when the connections are tested as cantilevers. The failure in this case takes place in the top weld (the weld connecting the top angle to the flange of the column), but only after the angle has deflected to such an extent that its heel has separated from the column flange about

* To be presented at Annual Meeting, AMERICAN WELDING SOCIETY, Atlantic City, October 18–22. A contribution to the Fundamental Research Division, Welding Research Committee. Report of Structural Steel Welding Research Committee.

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‡ Formerly American Welding Society Research Fellow, Lehigh University.

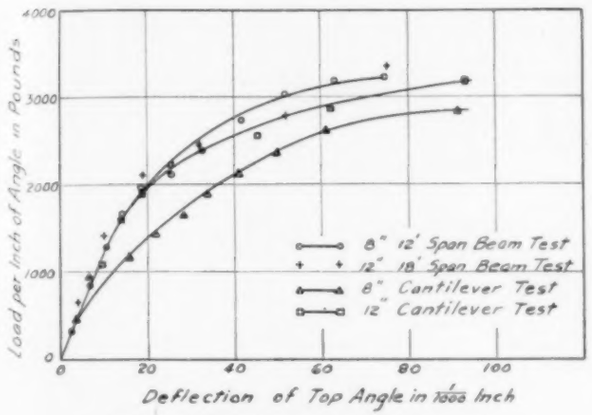

Fig. 3—Load Deflection Curves of 5/8-Inch Top Angle

one-half inch. The failure of this weld is primarily due to a bending action along its throat section. The weld connecting the top angle to the beam has little bending action and should be designed on the basis of the direct force along the leg. The weld connecting the lower flange of the beam and seat angle carries the compressive thrust of the lower flange which is induced by the restraint of the top angle. The welds connecting the seat to the column simply carry the vertical reaction of the beam.

Referring to Fig. 1, it is shown how the end moment of a beam or cantilever is resisted by a couple consisting of a thurst located approximately at the top of the seat angle and a tension pull on the top angle located at the edge of the top leg.

The force or load on the top angle depends on the depth of the beam and the end moment. In cantilever tests the end moment is determined directly, but in the beam tests the end moments are taken as the difference between the total external moment and the measured moment at the center of the beam. The corresponding deflections of the top angle are computed from the dial deflections as shown in the lower sketch of Fig. 1. From these two values a load deflection diagram of the top angle can be plotted, and these diagrams form the basis by which the existing conditions are studied.

The theoretical analysis of the bending of the top angle presented in the previous report showed that the top weld contributed about one-half the strength and stiffness of the top angle connections. A series of cantilever tests using sixteen-inch beams and 3 × 3 × 7/8-in. top

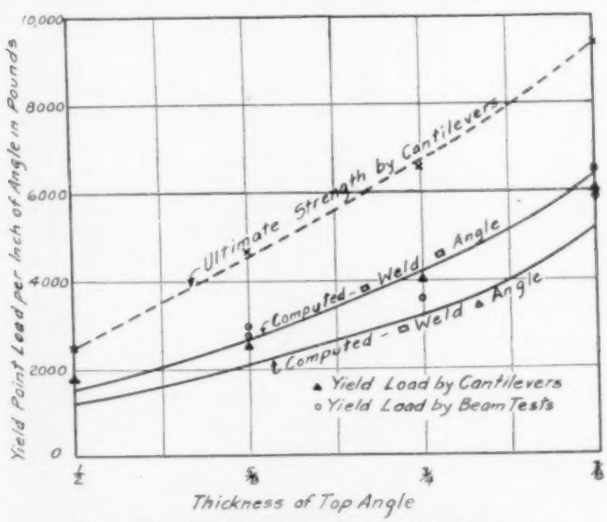

Fig. 4—Effect of Thickness of Top Angle and Yield-Point Load

angles were made to determine the effect of the size of the top weld. The results are plotted in Fig. 2 which shows that the stiffness, ultimate strength and deflection are considerably influenced by the size of weld. These tests seem to justify the theoretical analysis of the top angle and they definitely demonstrate that the size of the top weld should be equal to the thickness of the angle in order to provide a consistent factor of safety both in strength and flexibility. The load deflection curves of the top angles in Fig. 3 show that the angles have reached their yield point at a deflection of about 0.05 in. Two methods of computing the yield load of the angle were presented in the previous report; the following assumptions being made: (1) rectangular bending stress distribution across the throat of the weld with triangular bending stress distribution in the angles, (2) rectangular stress distribution in both weld and angles. The computed yield-point loads of the angles from this theory are plotted in Fig. 4. The experimental values fall in general between these two theoretical curves. The values computed using the assumption of rectangular distribution in the weld and triangular in the angles are the ones recommended for design because they provide a factor of safety of about 2 based on the ultimate strengths of the connections.

Fig. 5—Long Time Sustained Load of a Top Angle Connection

Effect of Sustained and Repeated Loading

All beams of spans greater than eight feet cause the bending stresses in the top angle and weld to exceed the yield point when designed for fifty per cent restraint. The question of the safety of this type of connection therefore needed further study. This was done in a series of tests with sustained and repeated loading. A long time test of a top angle connection of the cantilever type was made and the method of testing is shown in Fig. 5. The load was maintained by adjusting the calibrated loading spring, while the rotation of the connection was measured by the deflection dials. After the load was applied the top angle continued to deflect a small amount for a short time as shown in Fig. 6. The yield-point stresses in the top angle and weld increased the deflection until the stresses were adjusted and balanced the external moment. Let us designate this added deflection of the top angle as plastic flow or simply flow, so as to

distinguish it from "creep" of steel which is the time deformation produced by direct stress. The long time load test showed that the flow of the angle reached a maximum of only three per cent of the total deflection after about five hours of loading, although the load was maintained for twenty days. The test indicated that the flow would reach a maximum and the angle would become stable after a relatively short time under load.

In order to study the important variables which affect the amount of flow of the top angles, two cantilever specimens were loaded with a spring in the testing machine. The load was held for about five hours at various increments and the amount of flow was recorded and plotted as shown in Fig. 7. The amount of flow increased with the increase of deflection of the top angles in both specimens. As long as the deflection of the angle was less than 0.10 in. the flow stopped within five hours. The flow-time curves for each load interval are plotted in Fig. 8. Two beam tests were made using the same end connections as those used in the two cantilever specimens. The eight- and sixteen-inch beams had twelve- and eighteen-foot spans, respectively, and the connections were designed to give greater than fifty per cent restraint at a stress of 18,000 psi in the center of the beams. At design loads the beams showed no measurable increase in stress at the center, or appreciable rotation of the ends after the load had been held for twenty hours. The effect of flow is much less noticeable in complete beam tests because any small rotations caused by the flow of the top angles would cause the load to be taken up by small increases in stress at the center of the beams. The flow of top angles with time seems to be nothing more than a plastic stress adjustment in the material strained above its yield point and is not considered dangerous if the beams are limited in span so that at design load the deflection of the top angle is less than 0.07 in.

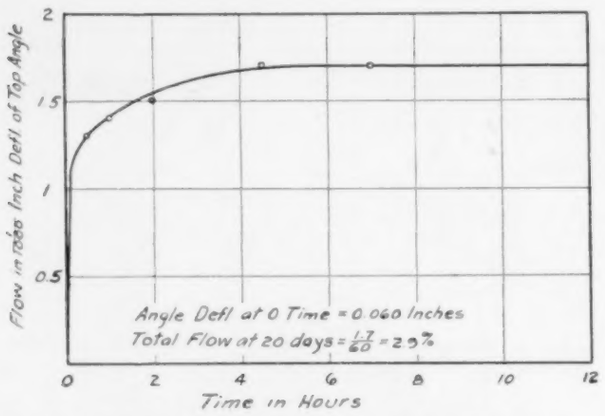

Fig. 6—Long Time Load Test of Top Angle Connection Beam B12-28 Lb. Top Angle 3" x 4" x 5/16"

Repeated load tests were made in the 300,000-lb. Olsen testing machine to determine any tendency of a progressive fracture in the top angle connection. Only relatively few repetitions were applied, but the deflection dials would show any tendency of progressive failure by an increase of angle deflection with the repetitions. The test represents simply the loading and unloading of the beams and is not a reversal of stress.

The load was repeated twenty times on the eight-inch cantilever specimen after every period of flow test. No further appreciable deflection of the angle could be measured until the period where the angle deflection

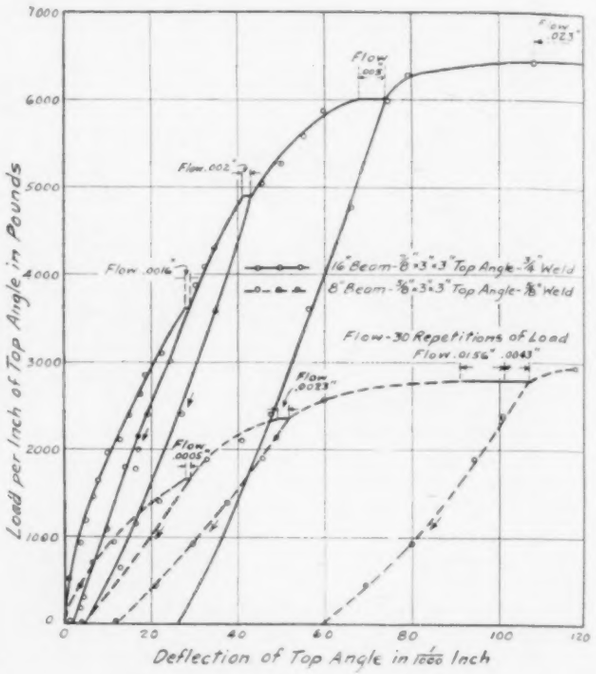

Fig. 7—Cantilever Flow Test with Spring Loading

was 0.09 in. After thirty repetitions at this load the angle had deflected 0.0043 in. as shown in Fig. 7. The deflections were measured after every fifth repetition and were found to be gradually less for each period. The design load was repeated ten times on both beam tests and no added deflections of the top angles could be detected. It seems, therefore, that repeated loading does not produce as much effect on the angle as the sustained load. The limit of angle deflection of 0.07 in. set by the

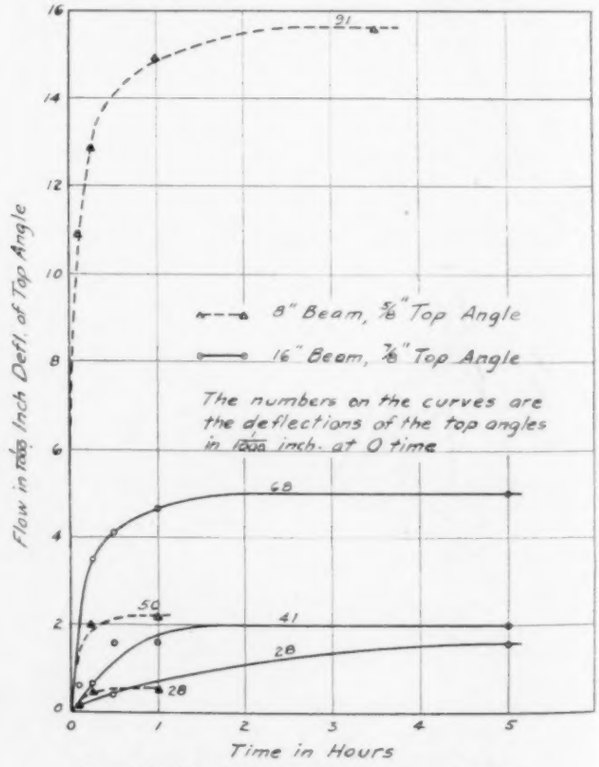

Fig. 8—Short Time Flow Tests with Spring Loading of Cantilevers

Fig. 9—The 8-In. Complete Beam-Column Test at Failure

flow studies seems, therefore, to be on the safe side for repeated loading. The effect of repeated loading is not as important as one might think for such high stresses because the yield-point stress of the material is not exceeded after the initial deformation and plastic stress adjustment have taken place.

Web Crippling of Beams Supported on Seat Angles

The question of transferring the reaction of the beams to the columns must also be considered. The primary function of the seat angle is to support the end of the beam, and the secondary function is to carry the compressive thrust of the lower flange which is caused by the end restraint induced by the top connection. The outstanding leg of the seat angle is welded to the flange of the beam in order to resist the compressive thrust The welds also act in a secondary function to prevent the seat angle from bending downward as the vertical load is applied along its outstanding leg. This increases the bearing value of the beam on the seat angle, even though the seat angle itself is relatively thin and flexible. The bending action on the seat angle and on the welds attaching it to the face of the column is further reduced by the eccentric action of the thrust of the lower flange of the beam. This action can be visualized by examining the sketch of the connection as shown in Fig. 1.

The test specimens used to study the bearing strength of beams supported on seat angles were of the cantilever type. Short lever arms were used so that the connection failed by web crippling of the beam above the seat. Plates were used as a top connection instead of top angles because they could take very little vertical reaction. The results of the tests are summarized in Table 1. At the ultimate load, the web of the beam crippled, and there was no sign of failure in any of the welds or seat angles. The tests indicate that maximum length of the outstanding leg of the seat angle should be about six inches because for greater lengths their stiffness decreased. A new design formula for beam reaction is proposed since the American Institute of Steel Construction recommendation* is a formula for web buckling and is not appropriate for web crippling. The primary failure in web crippling is the yielding of the web over the supports in direct compression which is followed by a rather sudden localized buckling of the web extending from the end of the support 45 degrees upward. However, the additional bearing length provided by the 45 degrees stress distribution has been neglected, so the proposed formula becomes:

$$R = 18{,}000\ t{\cdot}a$$

where R = permissible reaction,
t = web thickness, and
a = length of bearing.

The relatively high reaction strength of the 12-in. 45-lb. beam can be attributed to its thick flanges.

The length of the seat angle should be one inch greater than the width of the beam flange to provide space for the side welds. The tests indicate that the thickness of the seat angle should be designed for shear on a section of the leg with a factor of safety of $2^1/_2$ based on the yield point of the metal in shear. The length of the outstanding leg of the seat is determined either by the length required for bearing of the beam or by the length required to provide a sufficient length of weld to carry the compressive thrust of the flange of the beam. The welds connecting the seat angle to the column are required to carry only the vertical reaction of the beam in shear.

Complete Beam-Column Tests

Two beam specimens were designed for fifty per cent restraint and tested to their ultimate loads. An eight-

* New formula published since this writing of the paper. See A. I. S. C. Specification for the Design of Structural Steel for Buildings.

inch beam with a twelve-foot span had 3 x 3 x $^{5}/_{8}$-in. top angles $5^{3}/_{4}$-in. long, and a sixteen-inch beam with an eighteen-foot span had 3 x 3 x $^{7}/_{8}$-in. top angles 7-in. long to provide the desired restraint. The testing procedure employed was similar to that described in the previous report. Figure 9 shows the set-up in the testing machine for the eight-inch beam specimen. This picture was taken at the ultimate load when a decided curve in the beam could be noticed. The design load on the beam for a stress of 18,000 psi at its center was 17,200 lb. At a load of 36,000 lb. the flanges of the beam reached the yield point. The scaling of the whitewash

Table 1—Web Crippling of Beams on Seat Angles

	Beam Size	Beam Web Thickness In.	Seat Angle Length In.	Seat Angle Thickness In.	Seat Angle Outstanding Leg In.	Length of Bearing In.	Maximum Shear on Seat Angle Lb. per In.[2]	Maximum Reaction Lb.	Design Reactions A.I.S.C. * Lb.	Design Reactions Proposed † Lb.
B	12–45	0.336	9	$^{1}/_{2}$	4	$3^{1}/_{2}$	21,600	97,500	32,400	21,100
B	12–28	0.240	$7^{1}/_{2}$	$^{1}/_{2}$	4	$3^{1}/_{2}$	11,900	44,500	19,800	15,100
B	12–28	0.240	$7^{1}/_{2}$	$^{1}/_{2}$	6	$5^{1}/_{2}$	13,800	51,900	25,900	23,800
B	12–28	0.240	$7^{1}/_{2}$	$^{1}/_{2}$	8	$7^{1}/_{2}$	14,700	55,400	32,000	32,400
B	16–40	0.307	8	$^{1}/_{2}$	6	$5^{1}/_{2}$	13,600	54,400	36,100	31,400
B	8–17	0.230	6	$^{5}/_{8}$	4	$3^{1}/_{2}$	18,000	40,500	19,000	14,500

* A.I.S.C. Design Reactions for Beam Supported on Stiffened Seats:

$$f_b = \frac{18{,}000}{1 + d^2/6000t^2} \qquad R = f_b t \left(a + \frac{d}{4}\right)$$

† Proposed Design Reactions for Beams Welded to Seat Angles:

$R = 18{,}000\ t{\cdot}a$

a = length of bearing
t = web thickness
d = depth of beam
R = permissible reaction

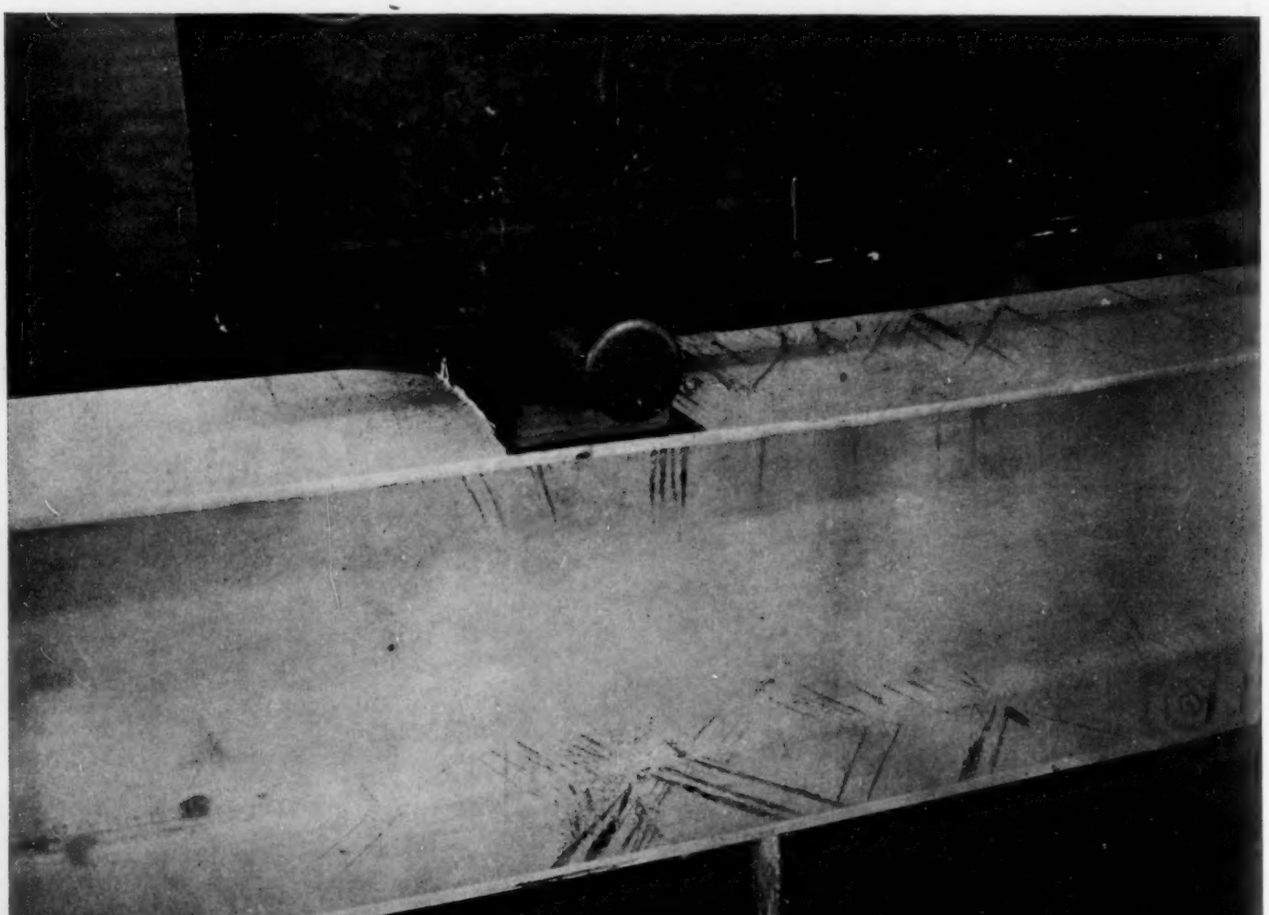

Fig. 10—Yielding of the Flanges of the 8-In. Beam Test at the Ultimate Load

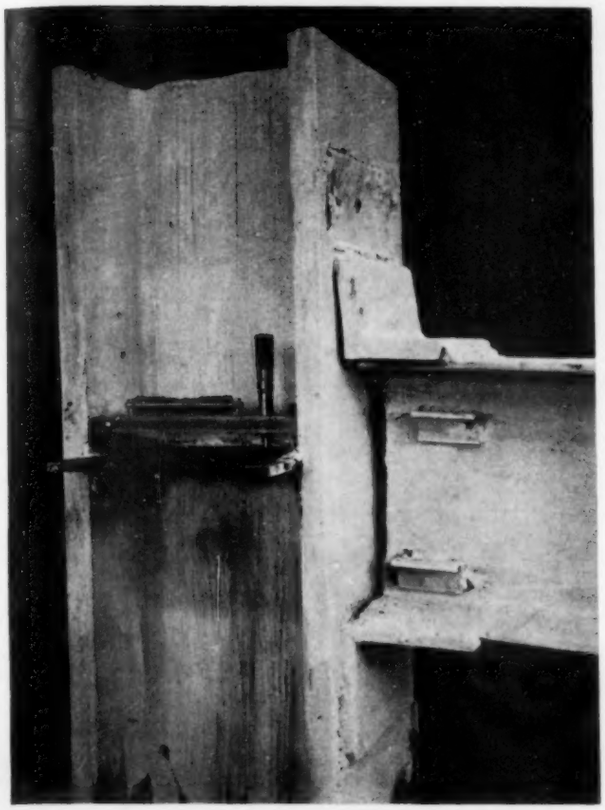

Fig. 11—The End Connection at the Ultimate Load on the 8-In. Beam Specimen

on the flanges between the loading points can be seen in Fig. 10. A factor of safety of more than 2 was thus obtained and that is all that is generally used for simply supported beams in structural steel design. At the ultimate load the top angle had deflected about $^3/_{16}$ in. as can be seen in Fig. 11. Since the top angle will not fail until it has deflected about $^1/_2$ in., there is ample margin of safety due to the fact that beams of this length will yield in flexure at the center before the end connection fails. The figure also shows the level bar which was used to check the plumbness of the columns. The jacks shown in Fig. 9 were used to keep the columns vertical.

The sixteen-inch beam specimen had no lateral support and was so long that excessive lateral deflection took place. The test was discontinued before the yield-point stresses in the flanges were obtained. In Fig. 12 the percentage rigidity is plotted against the stress in the center of the beam. This figure shows that both beams had greater than fifty per cent restraint at design loads.

Limits of Sizes and Spans of Beams for Top Angle Connections Giving Fifty Per Cent Restraint

The limit of the section modulus of beams for connections of this type depends on the length and the yield-point stress of the top angles, and on the depth of the beam itself. The ideal beams for top angle connections are those of the lighter type having fairly wide flanges. The amount of deflection of the top angle limits the spans of beams for these connections. The amount of deflection of top angles in beams connected for a given restraint at design loads is directly proportional to the span of the beams, assuming that the center of rotation of the end of the beam is at the seat angle. This assumption is fairly accurate as can be seen in Table 1 of the previous report. The beams rotate about a point slightly above the seat so the assumption is always on the safe side when computing top angle deflections. The span is limited by the amount of angle deflection at the design load on the beam. This minimum deflection is taken as about 0.030 in. The maximum span that seems advisable is that which produces a top angle deflection of 0.070 in. At this deflection the flow begins to become important and larger deflections might cause some danger due to repeated loading. For these limits of deflections, the corresponding limits of beam spans may be computed as follows:

Notation

M_c = moment at the center of the beam
M = end moment
θ = rotation of the end of the beam in radians
W = total load on beam
L = length or span of beams

Uniformly loaded beams at fifty per cent restraint:

$$M_c = \frac{WL}{12}$$

$$M = \frac{WL}{24}$$

From the slope deflection theory:

$$\theta = \frac{WL^2}{24EI} - \frac{ML}{2EI}$$

substituting for M:

$$\theta = \frac{WL^2}{48EI}$$

for a design stress of 18,000 psi at the center:

$$\frac{WL}{12} = \frac{sI}{c} = \frac{18{,}000I}{d/2} = \frac{36{,}000I}{d}$$

$$W = \frac{12 \times 36{,}000I}{Ld}$$

substituting W in the expression for θ and using E = 29,000,000 psi we have:

$$\theta = \frac{9L}{29{,}000d} \quad \text{or} \quad \theta d = \frac{9L}{29{,}000}$$

The deflection of the top angle is θd when the end of the beam rotates about its seat.

Minimum Span

$$\theta d = 0.030 \text{ in.}, \; L = \frac{29{,}000 \times 0.030}{9} = 97 \text{ in. or } 8 \text{ ft.}$$

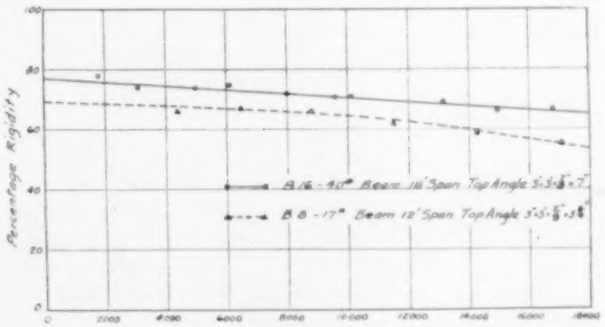
Percentage Rigidity
Stress in Center of Beam - p.s.i.
B 16 - 42# Beam 16' Span Top Angle 5"x5"x⅝"x7"
B 8 - 17# Beam 12' Span Top Angle 5"x5"x⅝"x8"

Fig. 12—Variations of Rigidity with Stress in Beams

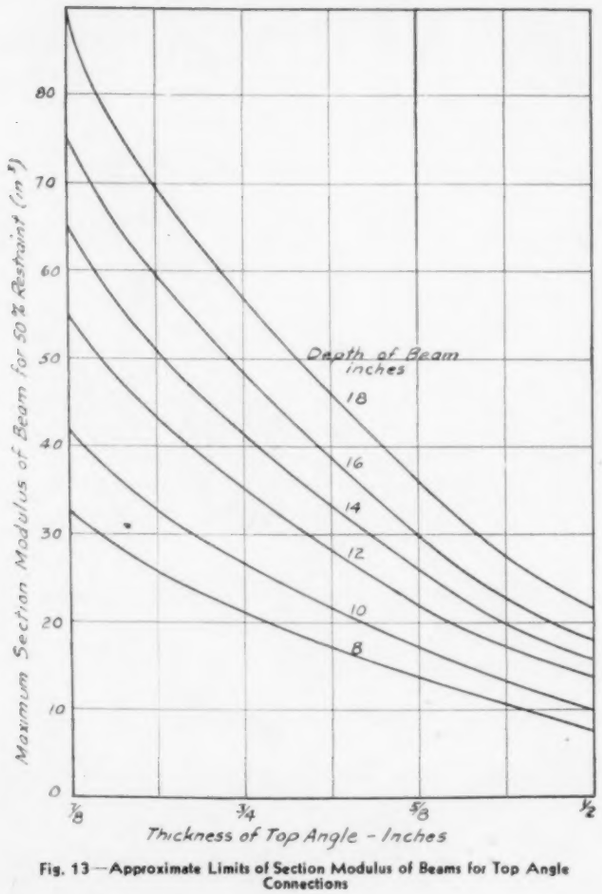

Fig. 13—Approximate Limits of Section Modulus of Beams for Top Angle Connections

Maximum Span

$\theta d = 0.070$ in., $L = \dfrac{29{,}000 \times 0.070}{9} = 226$ in. or 18.8 ft.

The limits of size of beams for fifty per cent restraint by top angles can be computed in the following manner:

Uniformly Loaded Beams

$$M = \frac{WL}{24} = a(d + 3)\, C$$

$$\frac{M}{s} = \frac{I}{c} = S$$

where

a = length of top angle and the width of the beam flange
C = design load per inch of top angle
S = section modulus of the beam

The maximum stress at the end of the beam will be one-half its center stress, or 9000 psi

$$\therefore M = 9000S$$

substituting for M:

$$S = \frac{a(d + 3)C}{9000}$$

Knowing the width of the flange and the depth of the beam the maximum section modulus can be computed for each size of top angle. Taking these dimensions from a series of average beams the maximum section moduli were computed and plotted in Fig. 13. Using a maximum 3 x 3 x $^7/_8$-in. top angle a fairly wide range of light weight beams can be used in this type of connection.

Proposed Method of Design

The method of design can be best illustrated by an example which is given below:

Example

Given: Span 16 ft. Total uniform load 40,000 lb.

Beam

Center moment $= \dfrac{WL}{12}$

$$S = \frac{M}{s} = \frac{WL}{12 \times 18{,}000} = \frac{40{,}000 \times 16 \times 12}{12 \times 18{,}000}$$

$S = 35.5$ in.³

using a 12 × 6$^1/_2$ 28-lb. beam, $S = 35.6$ in.³
web = 12 × 0.240 in. flange = 6.5 × 0.420 in.

Top Angle

End moment =

$$\frac{WL}{24} = \frac{40{,}000 \times 16 \times 12}{24} = 320{,}000 \text{ in.-lb.}$$

$$\text{Force on top angle} = \frac{M}{d+3} = \frac{320{,}000}{12 + 3} = 21{,}300 \text{ lb.}$$

$$\text{Force per inch} = \frac{21{,}300}{6.5} = 3280 \text{ lb.}$$

In Fig. 4, assuming a rectangular stress distribution for the weld and triangular for the angle, a $^3/_4$-in. angle gives a yield load of 3200 lb. which is close enough.

Use a 3 x 3 x $^3/_4$-in. top angle 6$^1/_2$-in. long

Top weld = $^3/_4$-in. fillet 5$^1/_2$-in. long
Beam weld = $^7/_{16}$-in. fillet 6$^1/_2$-in. long

Seat Angle

Proposed design shearing stress in leg of seat angle 8000 psi.

$$\text{Thickness of seat: } t = \frac{20{,}000}{7.5 \times 8000} = 0.333 \text{ in.}$$

Use $^3/_8$-in. thick seat angle

Try 4 x 6 x $^3/_8$-in. angle 7$^1/_2$-in. long.

Welds to the lower flange of beam allowing $^1/_2$-in. gap, available length 5$^1/_2$ x 2 = 11 in.

$$\text{Load per inch} = \frac{21{,}300}{11} = 1940 \text{ lb.}$$

Use 5$^1/_2$ in. of $^1/_4$-in. fillet on each side.
Welds to the column, available length = 8 in.

$$\text{Load per inch} = \frac{20{,}000}{8} = 2500 \text{ lb.}$$

Use 8 in. of $^3/_8$-in. fillet.

Check on Web Crippling

$R = 18{,}000 \times t_w \times 5^1/_2$
$R = 18{,}000 \times 0.240 \times 5.5 = 23{,}800$ lb.

The 6 x 4 x $^3/_8$-in. seat angles 7$^1/_2$-in. long are satisfactory.

This design does not consider either the localized or the bending effect of the connection on the columns. However, if the columns are heavy and have beams connected on each side the effect will be of minor importance and may be neglected as the effect of wind bracing is under present conditions. The proposed program for the next two years' investigation on welded beam connections is the study of the effect of restraint connections on the columns.

Summary and Conclusions

1. The design theory proposed in the previous report was substantiated in this supplementary investigation.
2. Sustained and repeated loading did not have an appreciable effect on the connection when the top angle deflection was not excessive.
3. The use of 3 x 3-in. top angles of various thicknesses will produce satisfactory beam connections providing for fifty per cent restraint.
4. The practical range of spans of beams for 3 x 3-in. top angle connections is between eight and eighteen feet.
5. The reactions of beams when welded to seat angles may be designed for the same loads as bearing on a stiffened seat.
6. Top angle connections are best adapted for light weight wide flange beams.

Spot Welding Characteristics of Some Copper-Base Alloys*

By D. K. CRAMPTON† J. J. VREELAND‡

THIS study of resistance welding properties of copper-base alloys was undertaken in connection with work planned by the Copper Alloys Subcommittee of the Welding Research Committee. It is pointed out that the work at present reported is not a finished investigation of the welding properties of the alloys used. Rather it is to be viewed as an introductory survey indicating the need for certain further and more detailed investigations, not only of the welding properties of these particular alloys, but of other copper-base alloys as well. Further, it seems necessary to investigate in more detail the development of suitable test procedures.

To date there has been relatively little published on spot and seam welding of copper alloys. Thomasson[1] lists a considerable number of alloys including quite a few copper base alloys which he has found commercially amenable to spot welding. He also states the conditions found suitable for welding these alloys. Hook[2] in his excellent review of the welding of copper alloys speaks briefly of the resistance welding of these materials. One of the leading resistance welding equipment manufacturers[3] has also published considerable data on approximate energy input required for copper alloys of various conductivities, as well as certain data on suitable electrodes and electrode pressures. These authors, however, do not go into great detail as to the exact procedures best suited for welding each of the various alloys, nor do they describe a suitable testing technique to be used as a basis of comparison of materials or of treatments and welding conditions.

For the present series of tests a number of copper-base alloys were selected within the range which has been or possibly could be commercially used with resistance welding methods. In order to keep to a reasonable minimum the number of variables to be investigated, the present series of tests was confined to one thickness only; namely, 0.036 in. Further, all materials after final rolling and annealing on that gage were given a final surface treatment by buffing with greaseless rouge. Such a surface treatment previously had been found to be well suited to spot welding and is a treatment which is readily reproducible and of such uniform characteristics as to probably eliminate variations which might result from other surface treatments. It was also thought expedient to use one size, style and alloy of electrode to eliminate any variables from such sources. The electrode tips were made from a copper-chromium alloy and in all cases had a $^3/_{16}$ in. diameter flat contact surface. The machine used was a standard commercial 150 kva. spot

Fig. 1—Testing Spot Welds

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[1] H. Thomasson, "Non-ferrous Metals Successfully Spot Welded," *Industry and Welding*, July 1937, page 34.

[2] Ira T. Hook, "The Welding of Copper and Its Alloys—A Review of the Literature to Jan. 1, 1936." JOURNAL OF AMERICAN WELDING SOCIETY Supplement—February and March, 1937.

[3] P. R. Mallory & Co., "Engineering Data—Resistance Welding Theory and Practice." 1937.

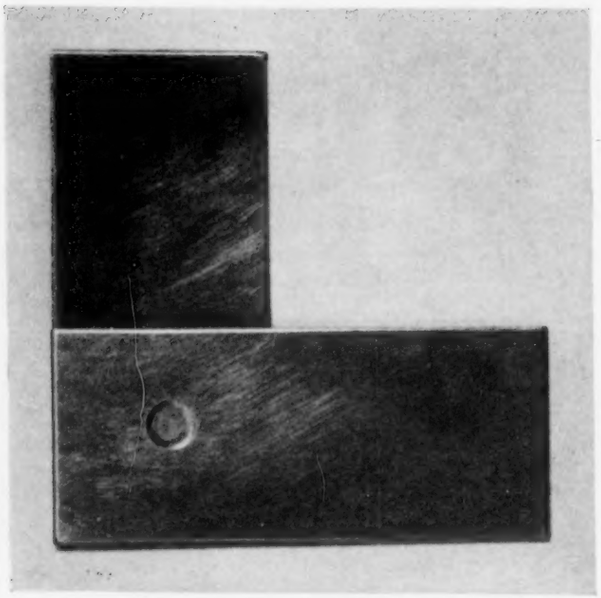


Fig. 2—Torsion Test Specimen

welding machine with balanced air operated head and having an electron tube timing control.

At the outset the problem of a suitable criterion for rating the various alloys or welding conditions presented itself. Inasmuch as no well recognized procedure had heretofore been worked out, it was necessary to empirically select what appeared to be suitable test requirements and develop these along with the investigation of the alloys themselves.

In the commercial application of these and other alloys, it has been quite common practice to determine roughly the range of welding conditions suitable for a given alloy and surface finish by ripping apart two welded test strips. The general procedure is illustrated in Fig. 1, which shows clearly the mode of testing and the usual type of failure of the samples. In most commercial work it has been considered, all that is necessary, is for the operator to know that the alloys will or will not make a reasonably strong weld. The operator merely adjusts welding conditions until the weld is sufficiently strong so that when tested in this manner it will "pull a plug," which simply means that the resistance to tearing of the welded zone itself is greater than the shear strength of the metal at the edge of the welded spot. This is shown clearly in the illustration.

Although the rough test just described is obviously incapable of showing slight differences in quality or strength of the weld, it was used in a preliminary survey of the materials here tested to give an approximate idea of the total range of conditions suitable for each alloy. Knowing what this approximate range was, certain finite sets of conditions within the total range were then selected for systematic investigation and testing by the torsion and hydrostatic tests. Earlier examinations of a torsion test had indicated it probably would be found a much more searching one than the preliminary test just described. The specimen finally adopted is illustrated in Fig. 2. It consists simply of two small strip specimens spot welded together at a single point, the axes of the individual specimens being at 90° to one another. For testing these specimens, a device illustrated in Fig. 3 was designed and built. The two portions of the welded specimen fit into machined grooves, one in a stationary plate and the other in a plate capable of rotation on a center coaxial with the center of the weld spot to be tested. By means of a cord at the periphery of the rotating disk and a spring balance, the maximum torque sustained by the spot weld could be measured.

In making up of the torsion specimens it was necessary to accurately align and center the spot so that when inserted in the testing machine it would coincide exactly with the center of the rotating disk. This centering of the spot on the specimen was accomplished by the use of a special bakelite jig made to fit over the electrodes of the welding machine.

The results of the torsion tests are shown in Table 1. There are listed here for each alloy tested the time, pressure and energy input both in terms of "heat number" and approximate kva. input. The figures given for torque are in all cases the average of four individual tests. The checks are quite good throughout. In addition to the torsion and hydrostatic bursting pressure tests, other indications of welding properties are furnished by the total thickness of the weld spot. In making the samples for torsion tests, this thickness was measured with a ball point micrometer for all conditions and is also recorded in Table 1.

It was felt that although the information obtained from the torsion test was very interesting and of considerable value in grading the materials and treatments, this test did not well simulate all conditions of service which might frequently be encountered in commercial use of the various alloys. In particular it was felt desirable to obtain information on the effect of variations in composition on the bursting pressure of a simple pressure vessel fabricated by spot welding. Therefore, from the

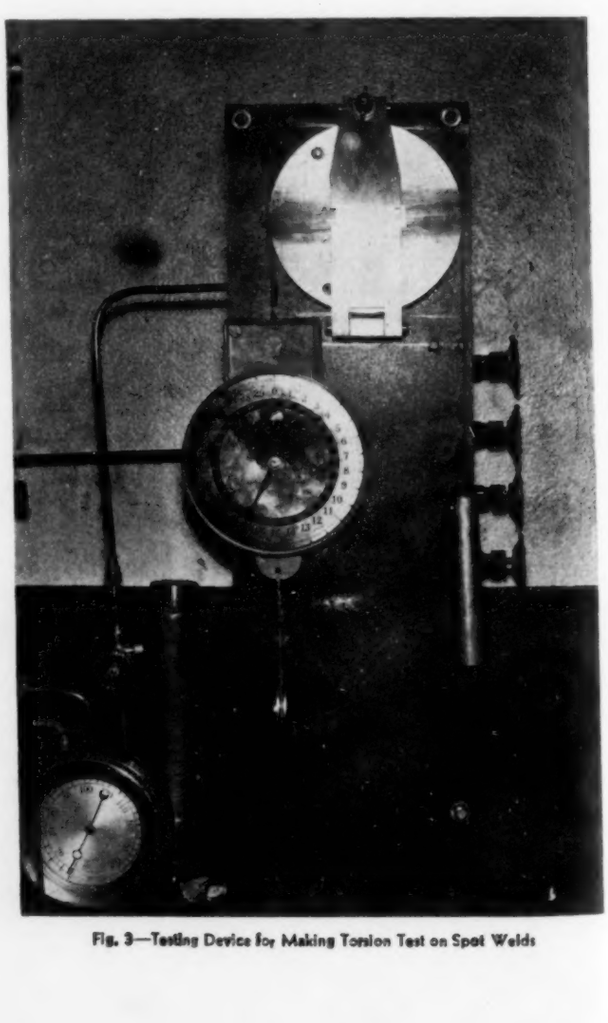


Fig. 3—Testing Device for Making Torsion Test on Spot Welds

results obtained using the torsion test, one set of welding conditions which appeared well suited to each alloy used was then selected for hydrostatic pressure testing. The basis of the selection of this set of conditions, while depending on the value of torque in the torsion test, was also influenced considerably by the type of failure and by the general behavior of the alloy noted during welding.

After considerable preliminary investigation it was decided that the simplest and most logical bursting pressure test could be made from two circular blanks with a series of spots accurately spaced on a circle somewhat smaller than the blank diameter. Instead of sealing the edges of the blanks by welding, soldering or other such means, a simple mechanical test jig was constructed to keep the edges pressure-tight so that the effect of the internal pressure was resisted by the weld spots. The details of the jig for testing are shown in Fig. 4. The specimen is shown clamped between the two parts of the jig. Water is pumped into the space between the two sheets through the tube shown, causing the sheets to bulge either side of the spot welds as indicated. A better idea of the specimen itself, before and after being subjected to the test, is gained from the actual photograph in Fig. 5. Usually failure was evidenced by a seepage

Table 1a—Muntz Metal

Analysis: Copper 60.0%, Zinc 40.0%

Cycles	Electrode Pressure Lb.	Heat No.	App. Kva. Input	Total Spot Thickness Inches	Torque Lb.-In.
2	400	12	90	0.068	51
		14	110	0.064	73*
		16	130	0.060	81*
4	400	11	80	0.066	70
		13	100	0.063	89*
		15	115	0.058	105*
6	400	10	70	0.064	40
		12	90	0.062	87*
		14	110	0.058	104*
2	510	15	115	0.067	57
		17	130	0.064	62
		19	155	0.060	78*
4	510	13	100	0.065	40
		15	115	0.060	71
		17	130	0.055	83*
2	625	17	130	0.064	55
		19	155	0.057	60
		21	180	0.052	79*
4	625	14	110	0.063	59
		16	130	0.058	70*
		18	145	0.054	86*

* On these tests the samples failed by shear at the edge of the weld area rather than by torsion in the weld.

Remarks

Alloy handles exceptionally well having a wide range of weldability.

No point sticking.

No unusual precautions necessary but due to the high amount of zinc, pressures lower than 400 lb. should be avoided.

Weld spot consists of 70 to 90% beta brass.

Table 1b—Common High Brass

Analysis: Copper 66.0%, Zinc 34.0%

Cycles	Electrode Pressure Lb.	Heat No.	App. Kva. Input	Total Spot Thickness Inches	Torque Lb.-In.
2	400	15	115	0.069	48
		17	130	0.062	58
		19	155	0.057	68
4	400	14	110	0.059	57
		16	130	0.055	86
		18	145	0.045	111*
6	400	12	90	0.060	61
		14	110	0.057	88*
		16	130	0.050	100*
2	510	17	135	0.056	54
		19	160	0.055	73
		21	180	0.053	84
4	510	14	110	0.060	52
		16	130	0.055	71
		18	145	0.050	89*
2	625	19	160	0.061	50
		21	180	0.059	65
		23	210	0.056	78*
4	625	15	115	0.057	63
		17	130	0.055	67
		19	155	0.049	80*

* On these tests the samples failed by shear at the edge of the weld area rather than by torsion in the weld.

Remarks

Alloy handles very well.

Very little point sticking except with high cycles and high pressures. Pressures should not be less than 400 lb. to minimize danger of arcing or blowing through especially at higher heats and longer times.

Table 1c—Cartridge Brass

Analysis: Copper 70.0%, Zinc 30.0%

Cycles	Electrode Pressure Lb.	Heat No.	App. Kva. Input	Total Spot Thickness Inches	Torque Lb.-In.
2	400	19	155	0.057	61
		21	180	0.054	68
		23	210	0.052	78
4	400	13	100	0.067	45
		15	115	0.060	68
		17	130	0.056	78
6	400	12	90	0.063	43
		14	110	0.055	58
		16	130	0.050	65
2	510	19	155	0.058	52
		21	180	0.056	59
		23	210	0.052	73
4	510	16	130	0.055	53
		18	145	0.052	63
		20	170	0.048	75
2	625	20	170	0.053	55
		22	195	0.049	66
		24	225	0.045	75
4	625	17	130	0.051	54
		19	155	0.048	63
		21	180	0.044	75

Remarks

Alloy more difficult to handle than high brass.

Very little point sticking at lower cycles and pressures.

Picking up of point material occurred at higher cycles, pressures and heats.

To prevent arcing or blowing through, pressures should not be less than 400 lb.

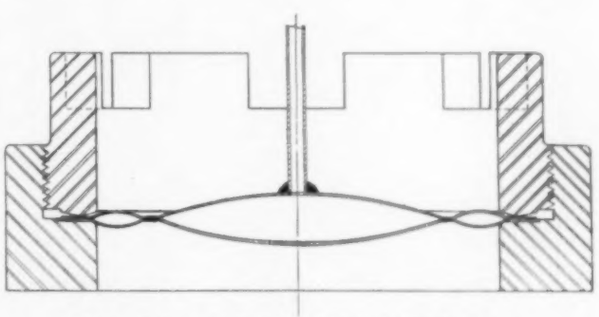


Fig. 4—Device for Testing Spot Welds in Hydrostatic Pressure

of water through an incipient shear failure at the edge of the actual fused zone. However, in certain instances a leak occurred through a porous weld at a pressure below that required to cause shear failure of the usual type.

Table 1d—Low Brass

Analysis: Copper 80.0%, Zinc 20.0%

Cycles	Electrode Pressure Lb.	Heat No.	App. Kva. Input	Total Spot Thickness Inches	Torque Lb.-In.
2	325	16	130	0.070	43
		18	145	0.068	46
		20	170	...	..
6	325	12	90	0.071	50
		14	110	0.065	68
8	325	12	90	...	..
		14	110	...	..
6	400	14	110	0.069	62
		16	130	0.060	88
8	400	15	115	0.059	65
		17	130	0.056	75

Remarks

Little trouble experienced with point sticking particularly on higher cycles, pressures and heats.

Table 1e—Red Brass

Analysis: Copper 85.0%, Zinc 15.0%

6	400	17	130	0.063	44
8	400	15	115	0.069	38
		17	130	...	..

Remarks

Higher cycles, pressures and heats gave trouble with excessive point sticking and picking up.

Table 1f—Manganese Red Brass

Analysis: Copper 84.0%, Manganese 1.0%, Zinc 15.0%

Cycles	Electrode Pressure Lb.	Heat No.	App. Kva. Input	Total Spot Thickness Inches	Torque Lb.-In.
2	325	16	130	0.068	52
		18	145	0.066	58
		20	170	0.063	69
10	325	12	90	0.068	59
		14	110	0.066	75
		16	130	0.065	100
2	400	20	170	0.064	60
		22	195	0.062	64
		24	225	0.059	81
4	400	17	130	0.068	65
		19	155	0.063	82
		21	180	...	..
6	400	14	110	0.062	58
		16	130	0.059	66
		18	145	0.055	100
8	400	15	115	0.058	74
		17	130	0.054	83
		19	155	...	..
10	400	13	100	0.067	40
		15	115	0.062	78
		17	130	0.057	96

Remarks

Considerable trouble with point sticking particularly after coating was formed on electrode tip.

Picking up of point material occurred with the higher cycles, pressures and heats.

Improvement in weldability over Red Brass apparently due to addition of manganese.

The results of the bursting pressure tests are given in Table 2.

It was realized that this test was somewhat empirical but by using a constant set of test conditions it was felt the results obtained logically could be applied for comparing alloys or various surface treatments or welding conditions, etc.

Table 1g—Aluminum Brass

Analysis: Copper 76.0%, Aluminum 2.0%, Zinc 22.0%

Cycles	Electrode Pressure Lb.	Heat No.	App. Kva. Input	Total Spot Thickness Inches	Torque Lb.-In.
2	400	15	115	0.071	51
		17	130	0.067	61
		19	155	0.064	76
4	400	12	90	0.069	53
		14	110	0.066	75
		16	130	0.062	104
6	400	12	90	0.071	73
		14	110	0.067	104*
		16	130	0.054	136*
2	510	16	130	0.070	52
		18	145	0.067	68
		20	170	0.064	75
4	510	13	100	0.070	52
		15	115	0.063	69
		17	130	0.059	78*
2	625	18	145	0.069	55
		20	170	0.066	66
		22	195	0.062	77
4	625	15	115	0.070	55
		17	130	0.063	72
		19	155	0.058	90

* On these tests the samples failed by shear at the edge of the weld area rather than by torsion in the weld.

Remarks

Alloy handles fairly well. Aluminum addition causes alloy to spit or flash under the points particularly at higher pressures.

Some point sticking but alloy could be handled commercially.

It was felt desirable to try to correlate electrical properties of the alloys with the welding characteristics and for this reason there are recorded in Table 3 the electrical conductivities of the alloys and the results of surface contact resistance measurements. Previous work in our laboratory and elsewhere has indicated an enormous effect of variations in surface condition on contact resistance as well as on weldability. As pointed out earlier in this paper, a single surface treatment was used throughout this work but the necessity of making complete and accurate studies of the quantitative effect of various surface treatments and textures is realized.

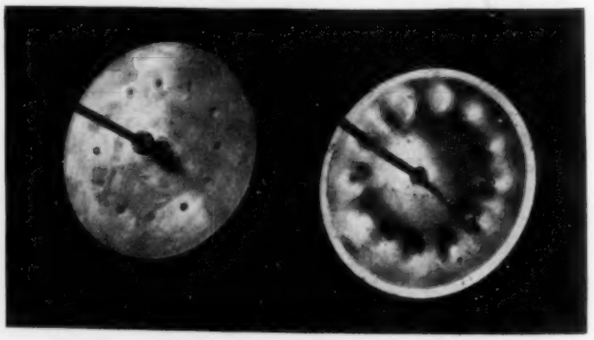


Fig. 5—Specimen Before and After Test

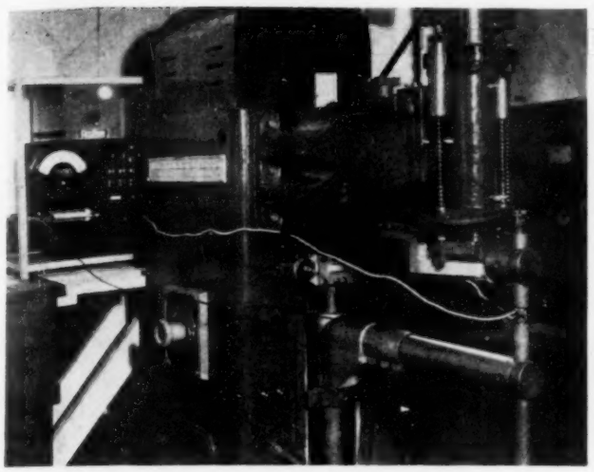


Fig. 6—Arrangement for Determining Contact Resistance

In order to determine the contact resistance, special electrodes were used with current leads from a low voltage D.C. line permanently connected to the electrodes near the tip. Two other leads from close to the tips of the two electrodes were run to a potentiometer for measurement of the actual e.m.f. drop. The general arrangement is shown in Fig. 6. In making these tests a current of 20 amps. was arbitrarily chosen and maintained throughout all the series of tests. Pressures were varied over a considerable range and the potential drop across the electrodes measured in millivolts. Results are given in terms of resistance in ohms. for the two surface contacts. Calculations showed the resistance of the strip itself was negligible as compared to the surface contact resistance.

Although these data on contact resistance at room temperature are interesting and instructive, it is realized that the relative values obtained do not necessarily hold under normal operating conditions and for conditions at the time of actual fusion. It is believed that for a complete understanding of the process, measurements of contact resistance at the instant of fusion should be made. Possibly under those conditions the resistance of the metal itself would become quite large in relation to the

Table 1h—Silicon Brass

Analysis: Copper 77.0%, Silicon 1.0%, Zinc 22.0%

Cycles	Electrode Pressure Lb.	Heat No.	App. Kva. Input	Total Spot Thickness Inches	Torque Lb.-In.
2	400	9	65	0.071	42
		11	80	0.070	63
		13	100	0.069	70
		15	115	0.065	79
4	400	8	60	0.069	39
		10	70	0.067	75
		12	90	0.062	95
		14	110	0.060	110*
6	400	8	60	0.069	52
		10	70	0.068	85
		12	90	0.057	106*
		14	110	0.056	125*
2	510	11	80	0.070	46
		13	100	0.069	60
		15	115	0.065	69
		17	130	0.061	91
4	510	9	65	0.070	40
		11	80	0.068	76
		13	100	0.065	96
		15	115	0.064	110*
6	510	10	70	0.070	64
		12	90	0.060	84
		14	110	0.057	110*
		16	130	0.054	120*
2	625	13	100	0.070	56
		15	115	0.067	69
		17	130	0.062	80
		19	155	0.059	93*
4	625	11	80	0.070	57
		13	100	0.063	78
		15	115	0.058	91
		17	130	0.055	97
6	625	10	70	0.070	39
		12	90	0.062	90
		14	110	0.061	95
		16	130	0.057	129*

* On these tests the samples failed by shear at the edge of the weld area rather than by torsion in the weld.

Remarks

Alloy has excellent welding qualities with a wide welding range. Very little point sticking and no point pick up at any time.

Table 1i—Silicon Bronze

Analysis: Copper 96.0%, Silicon 3.0%, Zinc 1.0%

Cycles	Electrode Pressure Lb.	Heat No.	App. Kva. Input	Total Spot Thickness Inches	Torque Lb.-In.
2	400	7	50	0.066	58
		9	65	0.063	70
		11	80	0.060	90
		13	100	0.050	91*
4	400	6	45	0.068	66
		8	60	0.064	82
		10	70	0.058	101*
		12	90	0.052	115*
6	400	4	30	0.072	33
		6	45	0.065	71
		8	60	0.062	101
		10	70	0.049	99*
8	400	4	30	0.071	48
		6	45	0.064	70
		8	60	0.057	95
		10	70	0.050	113
4	510	6	45	0.070	58
		8	60	0.064	73
		10	70	0.061	96
		12	90	0.053	111*
6	510	4	30	0.071	31
		6	45	0.069	66
		8	60	0.063	89
		10	70	0.055	106
8	510	5	35	0.070	56
		7	50	0.067	83
		9	65	0.056	99
		11	80	0.053	112
4	625	5	35	0.070	20
		7	50	0.069	70
		9	65	0.065	84
		11	80	0.051	103*
6	625	6	45	0.071	70
		8	60	0.066	74
		10	70	0.063	98
		12	90	0.052	111*
8	625	5	35	0.070	28
		7	50	0.065	75
		9	65	0.060	99
		11	80	0.050	111*

* On these tests the samples failed by shear at the edge of the weld area rather than by torsion in the weld.

Remarks

Alloy has excellent welding properties with a very wide welding range.

Slight point sticking occurred. No point pick up at any time. Short time (2 to 4 cycles per spot) causes shrinkage voids to occur because weld spot is close to one surface of sheet.

contact resistance. Such measurements would probably yield information of considerable value in determining the reasons for difference in behavior of different alloys or for any given alloy with varying surface treatments.

Discussion of Results

Torsion Tests

The torsion test appears to be a marked improvement over the simple ripping test so commonly used commercially. Its superiority lies in the fact that it appears to be capable of differentiating between degrees of perfection of weld, while the ripping test simply gives a qualitative indication of the presence or absence of a weld. It is believed the torsion test will be found an excellent one for examining the effect of sizes or shapes of welding tip, varying thicknesses of metal or different surface finishes. As far as the presently observed data go, the following general conclusions may be drawn and these apparently apply to all alloys within the range of conditions here examined:

1. Torque increases markedly with increasing energy input.

Table 1j—Phosphor Bronze

Analysis: Copper 95.0%
Tin 5.0%

Cycles	Electrode Pressure Lb.	Heat No.	App. Kva. Input	Total Spot Thickness Inches	Torque Lb.-In.
2	400	12	90	0.072	55
		14	110	0.067	83
		16	130	0.065	99
4	400	11	80	0.072	71
		13	100	0.071	103
		15	115	0.068	129
6	400	9	65	0.073	34
		11	80	0.072	104
		13	100	0.070	124
8	400	8	60	0.073	54
		10	70	0.072	111
		12	90	0.069	140
4	510	10	70	0.073	56
		12	90	0.070	102
		14	110	0.064	118
6	510	9	65	0.072	63
		11	80	0.071	118
		13	100	0.067	132
8	510	8	60	0.073	40
		10	70	0.071	104
		12	90	0.065	128
4	625	11	80	0.073	32
		13	100	0.072	85
		15	115	0.071	111
6	625	10	70	0.073	41
		12	90	0.072	98
		14	110	0.068	146
8	625	9	65	0.071	43
		11	80	0.070	125
		13	100	0.056	170

Remarks

Alloy welds nicely giving exceptionally high values for torque test.
Points stick and pick up very badly.
Weld area brittle at low cycles and heats.

2. Torque increases appreciably with increase in number of cycles per spot.

3. Torque decreases somewhat with increased electrode pressure.

From the above and in the absence of any other considerations, it might well be concluded that best results would in general be obtained by using a high energy input, a large number of cycles per spot and a low electrode pressure. Unfortunately, however, there are distinct limitations to such a course not apparent from the actual test data recorded. We find that going too far in the directions indicated will inevitably lead to one or more of the following difficulties:

(*a*) There is an increased tendency for the electrode to stick to the sheet, which in turn leads to burning or "picking up" of electrode points. This is a serious matter from the commercial standpoint as it means excessive lost time in repeated dressing of points.

(*b*) There is an increased tendency for the electrodes to penetrate deeply into the sheet. This may lead to a greater possibility of leakage and tends toward a shear type of failure around the weld spot rather than failure by torsion in the weld itself. This action is particularly likely to occur on Muntz Metal or other alloys which are very soft at temperatures near their melting point. Additions of aluminum also seem to accentuate this.

(*c*) With high zinc bearing alloys and possibly some others, there is a great tendency for "spitting" or "flashing" of metal between the two contact surfaces of the sheet. In exaggerated cases this may even result in complete burning through of the spot with explosive violence. The use of low electrode pressures accentuates this con-

Table 1k—Nickel-Aluminum Bronze—Work Hardening Type

Analysis: Copper 92.0%
Nickel 4.0%
Aluminum 4.0%

Cycles	Electrode Pressure Lb.	Heat No.	App. Kva. Input	Total Spot Thickness Inches	Torque Lb.-In.
2	400	12	90	0.066	47
		14	110	0.063	64
		16	130	0.060	74
4	400	10	70	0.068	49
		12	90	0.066	69
		14	110	0.062	81
6	400	9	65	0.069	51
		11	80	0.064	71
		13	100	0.062	102
8	400	8	60	0.065	58
		10	70	0.061	68
		12	90	0.055	89
4	510	10	70	0.071	52
		12	90	0.066	61
		14	110	0.061	71
6	510	10	70	0.063	55
		12	90	0.057	57
		14	110	0.053	96*
8	510	9	65	0.068	52
		11	80	0.062	62
		13	100	0.056	82*
4	625	11	80	0.068	57
		13	100	0.063	58
		15	115	0.058	67*
6	625	12	90	0.064	62
		14	110	0.060	71
		16	130	0.058	87*
8	625	11	80	0.061	64*
		13	100	0.056	71*
		15	115	0.054	87*

* On these tests the samples failed by shear at the edge of the weld area rather than by tortion in the weld.

Remarks

Alloy handled exceptionally well.
Practically no point sticking.
High heats and pressures gave excessive spitting or flashing.

Table 1l—Nickel-Aluminum Bronze—Precipitation Hardening Type

Analysis: Copper 91.0%, Nickel 7.5%, Aluminum 1.5%

Cycles	Electrode Pressure Lb.	Heat No.	App. Kva. Input	Total Spot Thickness Inches	Torque Lb.-In.
2	400	13	100	0.073	50
		15	115	0.067	60
		17	130	0.066	71
		19	155	0.064	76
4	400	10	70	0.070	56
		12	90	0.065	57
		14	110	0.060	73
		16	130	0.060	93
6	400	9	65	0.072	44
		11	80	0.066	65
		13	100	0.064	81
		15	115	0.058	95
8	400	8	60	0.072	57
		10	70	0.065	65
		12	90	0.061	76
		14	110	0.056	97
4	510	12	90	0.068	59
		14	110	0.066	67
		16	130	0.061	84
		18	145	0.056	90
6	510	10	70	0.070	50
		12	90	0.064	67
		14	110	0.061	69
		16	130	0.056	81
8	510	11	80	0.070	69
		13	100	0.066	73
		15	115	0.062	84
		17	130	0.059	99
4	625	12	90	0.071	55
		14	110	0.068	63
		16	130	0.060	70
		18	145	0.056	80
6	625	11	80	0.070	55
		13	100	0.058	64
		15	115	0.056	81
		17	130	...	..
8	625	8	60	0.069	25
		10	70	0.065	81
		12	90	0.056	62
		14	110	...	..
4	510	16	130	0.062	112

Aged 1½ hrs. at 900° F. and Air Cooled.

Remarks

Considerable trouble with point sticking and picking up after use due to oxide coating on electrode tip.

Higher heats, cycles and pressure should be avoided to minimize this trouble.

Table 2—Bursting Pressures

Alloy	Cycles	Pressure Lb.	Heat No.	App. Kva. Input	Bursting Pressure Lb.	Remarks
Muntz metal	4	400	13	100	235	
High brass	4	400	16	130	230	
Cartridge brass	4	400	17	130	205	
Low brass	6	400	16	130	250	
Red brass	6	400	17	130	260	
Manganese red brass	6	400	14	110	300	
Aluminum brass	4	400	16	130	310	
Silicon brass	4	510	13	100	385	
Silicon bronze	6	400	8	60	305	Note the slight drop in pressures (bursting) as welding pressures increase
	8	400	10	70	380	
	8	510	10	70	365	
	8	625	10	70	335	
Phosphor bronze	6	510	11	80	275	Low values due to poor ductility of fused metal
Nickel alum. brz. work hard.	6	400	11	80	320	
	6	400	13	100	330	
Nickel alum. brz. prec. hard.	4	510	13	100	330	
	6	400	13	100	300	Age-hardened 1½ hrs. at 900° F. Air Cooled. Low value due to poor ductility

dition, and in general pressures less than 400 lb. should not be used with these alloys.

Bursting Pressure Tests

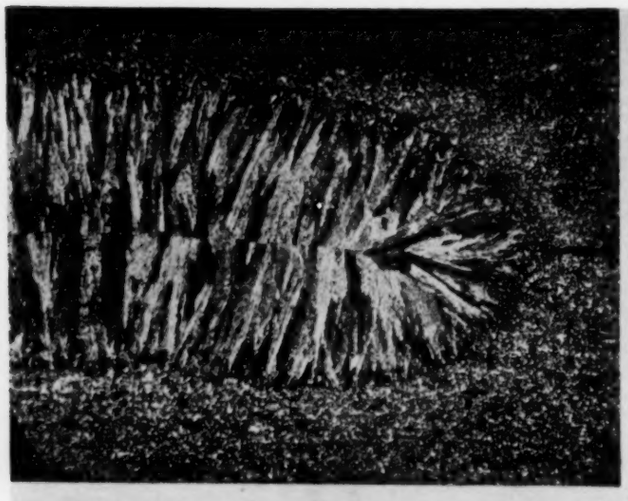
It is regretted that time was not available for a complete examination of the effect of all the welding conditions on bursting pressures of all the alloys examined. It was found necessary simply to take one set of conditions which appeared to be well suited for each alloy and make rupture tests on these. These bursting pressure data are quoted, therefore, with some hesitation and emphasizing the fact that they are included mainly to indicate the general possibilities of the test. Readers are cautioned not to use these data at face value as indicating the precise relative merits of the different alloys as further and more exhaustive tests might warrant the drawing of quite different conclusions in this respect. Attention is however called to the pressure tests of the Silicon Bronze where tests were made with constant energy input and constant number of cycles per spot but with three different electrode pressures. These data indicate an appreciable lowering of bursting pressure with increase of welding electrode pressure. This appears consistent with the results of the torsion test above cited.

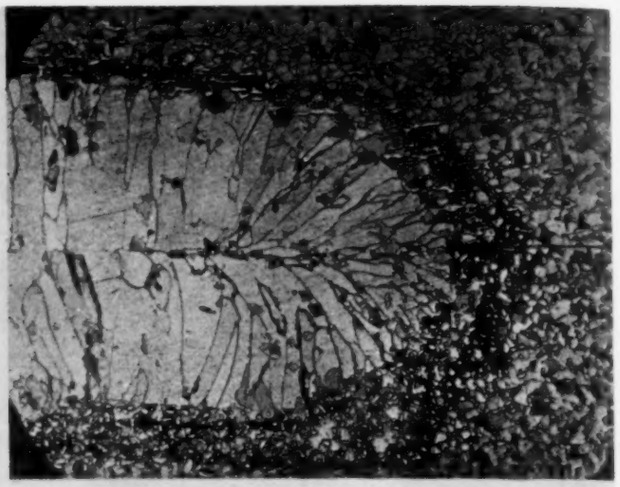
Table 3—Surface Resistance Measurements

Alloy	Amps.	Pressure Lb.	Emf. Millivolts	Contact Resistance Ohms. × 10^{-4}	App. Elec. Cond. % Iacs.
Muntz Metal	20	325	1.133	0.57	28
		675	0.592	0.29	
		975	0.374	0.19	
High Brass	20	325	1.284	0.64	26
		675	0.498	0.25	
		975	0.303	0.15	
Cartridge Brass	20	325	0.857	0.43	27
		675	0.488	0.25	
		975	0.301	0.15	
Low Brass	20	325	0.999	0.50	33
		675	0.423	0.21	
		975	0.297	0.15	
Red Brass	20	325	0.974	0.49	38
		675	0.396	0.20	
		975	0.275	0.14	
Manganese Red Brass	20	325	0.954	0.48	20
		675	0.614	0.31	
		975	0.385	0.19	
Aluminum Brass	20	325	1.097	0.55	23
		675	0.657	0.33	
		975	0.371	0.18	
Silicon Brass	20	325	1.129	0.56	13
		675	0.923	0.46	
		975	0.598	0.29	
Silicon Bronze	20	325	1.504	0.75	7
		675	0.723	0.36	
		975	0.557	0.28	
Phosphor Bronze	20	325	1.851	0.92	18
		675	1.205	0.60	
		975	0.632	0.32	
Nickel-Alum. Bronze-Work Hardening Type	20	325	0.809	0.40	15
		675	0.755	0.38	
		975	0.474	0.24	
Nickel-Alum. Bronze-Precipitation Hardening Type	20	325	1.103	0.55	13
		675	0.608	0.30	
		975	0.434	0.22	

Surface Contact Resistance

Results of measurements on contact resistance are listed in Table 3, together with the known values for the





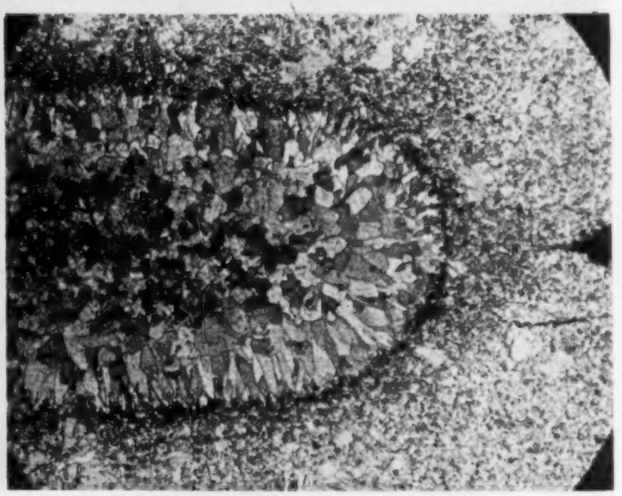
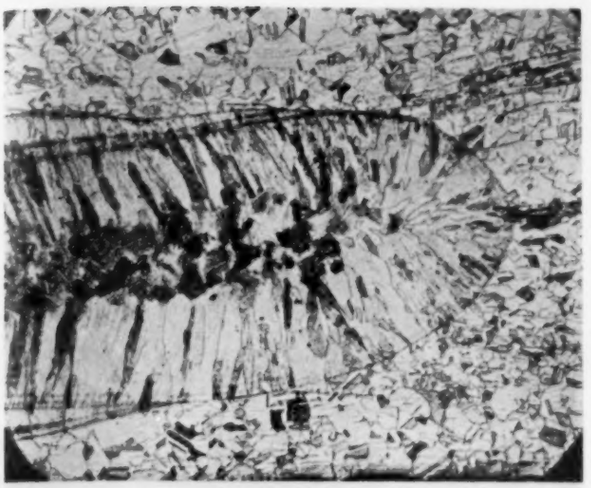
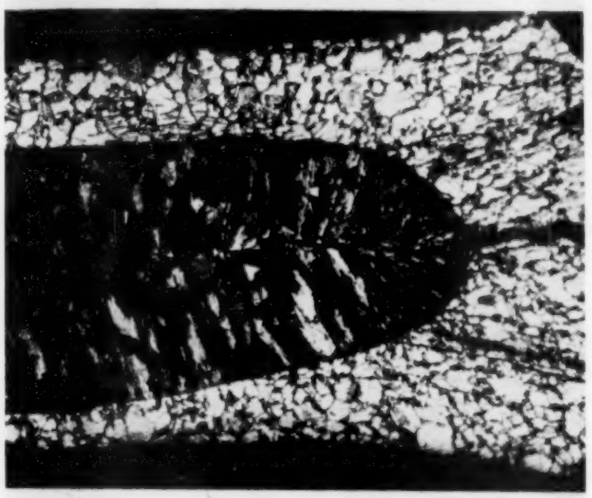


Fig. 7 (Top)—Weld in Silicon Bronze
Fig. 8 (Center)—Weld in Silicon Brass
Fig. 9 (Bottom)—Quick Weld in Silicon Brass

Fig. 10 (Top)—Weld in Muntz Metal (50X)
Fig. 10A (Center)—Same as Fig. 10 (250X)
Fig. 11 (Bottom)—Weld in Phosphor Bronze

electrical conductivity of each alloy. It had been thought that there would appear some direct connection between these two properties but the results actually found fail to disclose any such simple relationship. As will be seen from Table 3 the contact resistance falls off very markedly with increase of electrode pressure which is what might reasonably be expected. Most of the alloys examined showed contact resistances of the same general range of magnitude but it is interesting to note that two of the alloys showing the best all-around welding properties in other respects have decidedly the highest contact resistances. These are the Silicon Bronze and the Phosphor Bronze. The Silicon Bronze alloy is indeed the one of lowest electrical conductivity examined. How-





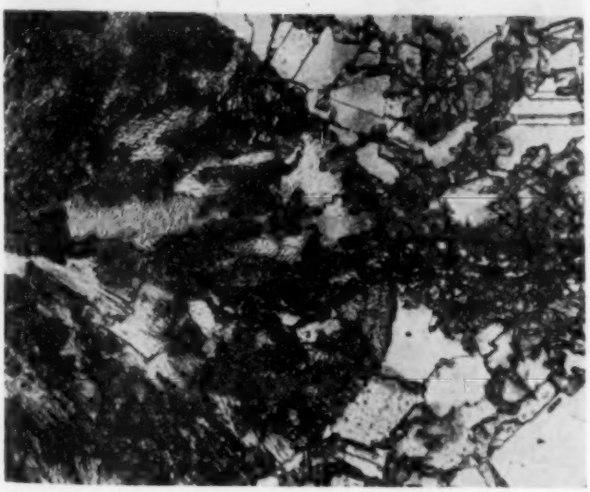


Fig 12 (Top)—Nickel Aluminum Bronze Weld Not Heat Treated
Fig. 13(Center)—Similar Weld as Fig. 12 After Aging Heat Treatment 50X
Fig. 13A (Bottom)—Same as 13 (250X)

ever, the Phosphor Bronze, which showed by far the highest surface contact resistance, is not as low as several others which showed much lower contact resistances.

It is felt that there is need for much further study along these lines and particularly there is need for information on the contact resistances at the temperatures and under the precise conditions obtaining at the instant of fusion. Quite probably under such conditions the resistances found will appear of greater significance in indicating weldability than the data here recorded. It is felt that under such conditions the relationship between the electrical resistivities of the several alloys and their surface contact resistances will be quite different from those here shown.

Microstructure

Microstructural characteristics of each individual alloy and for all conditions of welding were not examined in this series. However, quite a number of these were examined microscopically and Figs. 7 to 13 inclusive, are shown to indicate some of the more interesting and significant features disclosed. It is felt that detailed microstructural examination of spot welds at both high and low powers is a most valuable method for studying different welding procedures or for comparing various alloys. Sufficiently detailed and complete examination of this type was not made on the present series to warrant any general conclusions as to the importance of various variables in producing various structures.

Figure 7 shows the structure of a typical weld in Silicon Bronze. The joint between the two sheets of metal is shown at the right leading up to the end of the fused area itself. The base metal shows a very fine equiaxed structure and the weld zone shows long columnar grains meeting at the center. The line through the center of this zone is not evidence of any discontinuity but simply shows where the large grains butt against one another.

Figure 8 shows the typical structure of Silicon Brass welded with six cycles and 70 kva. input. This structure is quite similar to that of Fig. 7 and is typical of the welds giving best results in the mechanical tests on such alloys.

Figure 9 is of the same alloy, Silicon Brass, butt welded with two cycles and 130 kva. input under the same pressure as that of Fig. 7. It is seen that columnar crystals do not go to the center of the weld zone but the central portion is composed of more or less equiaxed crystals.

Figure 10 is Muntz Metal and shows a very interesting structure. The outer zone is, of course, the unaltered structure of the original sheet. The inner portion of the fused area shows large columnar beta brass crystals with a slight fringe of precipitated alpha. There is an intermediate zone, dark in the photomicrograph, which at higher magnification would show the presence of incipient fusion of the original alpha crystals, the small dark areas being composed of a typical cast alpha-beta structure. Figure 10*A* is the same as Fig. 10 except the magnification here is 250× instead of 50×. The left hand portion is the fused area and shows in better detail the fine alpha precipitating out from the large beta grains. In the right-hand portion of the picture the dark areas show fairly clearly the typical acicular alpha-beta structure of cast Muntz Metal.

Figure 11 shows structure of Phosphor Bronze with a rather sharp transition of the original structure to the fused area. Particular attention is called to the crack at the right of the fused zone just below the joint of the two original sheets, which is typical of a number of cracks found in this alloy under many conditions. A possible explanation of this phenomenon in this particular alloy is that it is somewhat hot short and a portion of the metal near the actual weld zone may have been heated to within the brittle range so that a slight movement of the metal caused fracture.

Figures 12, 13 and 13*A* are of the age-hardening type

Nickel-Aluminum Bronze. Figure 12 shows a typical weld not heat treated. It is characterized by long columnar crystals and a central portion of equiaxed crystals. Near the center of each original strip there are shown a series of wavy horizontal lines which are the ghosts of dendrites resulting from incomplete homogenization of the alloy in the early stages of working. Figure 13 shows a similar weld after an aging heat treatment which has caused a heavy precipitation within the weld zone and a considerable amount of precipitation at grain boundaries in the original weld metal. Figure 13*A* shows the same position at a magnification of 250× instead of 50×. The precipitated phase is just barely resolved in this magnification but shows a little more clearly than in the lower power pictures.

Behavior of Individual Alloys

Muntz Metal

This alloy is very easy to handle and can be welded over a considerable range of conditions. There is practically no tendency for points to stick. The torque is high but bursting pressure rather low which is due to a high percentage of beta brass in the weld. There is a very strong tendency for this alloy to fail by shear rather than torsion.

Common High Brass

A considerable range of welding conditions is permissible. The required energy input is, however, on the high side. There is very little point sticking except with longer welding time and high energy input. The torsional strength is reasonably high but the bursting pressure fairly low.

Cartridge Brass

This alloy is quite similar to high brass but in general somewhat more difficult to weld. Even poorer values on torsion and bursting pressures were found. The greatest objection is the strong tendency for the weld to blow through (explode) at longer welding times and with higher energy input and low welding pressures.

Low Brass

This alloy is quite difficult to weld in comparison with many of the others. It requires a low electrode pressure and fairly high energy input. There is some difficulty with point sticking and the torque and bursting pressures are comparatively low. The generally poor results are apparently the result of the comparatively high electrical conductivity of the alloy. Further work with different size and type of electrodes should be carried out.

Red Brass

The same objections can be raised to this alloy as to the low brass but the difficulties are even greater. Test values are quite low. Further work with different size and type of electrodes should be carried out.

Manganese-Bearing Red Brass

A marked improvement in welding quality results from the addition of manganese to red brass. A somewhat wider range of welding conditions appears suitable although there is also exhibited a great deal of difficulty with point sticking. Actual test values are quite high.

Aluminum Brass

This alloy handles fairly well over a considerable range of welding conditions but there is considerable difficulty with point sticking and with metal "spitting," especially at the higher pressures. Actual test values for both torsion and bursting pressure are very good.

Silicon Brass

This alloy has excellent welding properties over a very wide range. Quite low energy input is adequate. There is very little point sticking but quite a tendency for torsion specimens to fail by shear rather than torsion. The actual torsion test and bursting pressure results are among the highest observed.

Silicon Bronze

Considered from all angles, this appears to be the best alloy for welding of all those investigated. It can be welded over a wide range of conditions and especially can be welded with much lower energy input than any of the others. There is but a very slight tendency for points to stick and very high test values are observed for both torsion and bursting pressure tests.

Phosphor Bronze

In general, the ease of handling and weldability of this alloy runs close to the Silicon Bronze and about equal to the Silicon Brass. The range of conditions permissible is wide and the energy input required relatively low. The torsion test values are very high but the bursting pressure only moderately so due to the low ductility of the weld.

Nickel-Aluminum Bronze (Work-Hardening Type)

This alloy shows a fairly wide range of permissible welding conditions, very little tendency for sticking of points and fairly high torque and bursting pressure. There is a strong tendency for this alloy to fail by shear instead of torsion especially with the higher energy inputs, higher pressures and longer time.

Nickel-Aluminum Bronze (Precipitation-Hardening Type)

This alloy shows a fairly wide range of permissible welding conditions. There is a considerable tendency for electrode points to stick particularly at the higher energy inputs. Torsional strength values and bursting pressures are moderately high. In the aged condition an appreciable increase in torque is noted but, if anything, an apparent drop in bursting pressure. This latter is attributed to a lower ductility resulting from aging.

X-ray Methods of Studying Stress Relief in Welds*

By JOHN T. NORTON†

THE problem of residual stresses in welds and the determination of the proper treatment necessary to relieve these stresses under a particular set of conditions is of considerable practical importance. Furthermore, a non-destructive method of examination is a practical necessity if a general application is to be possible. X-ray diffraction methods are capable of yielding useful information in this connection and it is the purpose of this paper to discuss the type of information obtainable together with its possibilities of practical use.

The X-ray diffraction method, in common with most other methods, measures strain instead of stress so that the discussion will be based upon internal strains rather than internal stresses. The application of the method depends upon the fact that the appearance of the image produced when an X-ray beam is diffracted by a metal crystal is indicative of the condition of strain in the crystal itself.

An earlier paper on the same subject[1] discussed in detail a means of observing plastic strains but pointed out that the method was incapable of indicating the presence of internal elastic stress. Since that time there have been great advances in X-ray diffraction technique and it has been shown that elastic strains can be observed and measured with a fair degree of accuracy.

The newer X-ray technique is different from the old in that it uses the surface of a solid sample from which the X-ray beam is reflected rather than a very thin section through which the beam passes. The experimental arrangement is shown in Fig. 1. In this set-up the X-ray beam coming from the target is collimated by two pinholes to make a narrow pencil which illuminates a spot about two mm. in diameter on the specimen. The

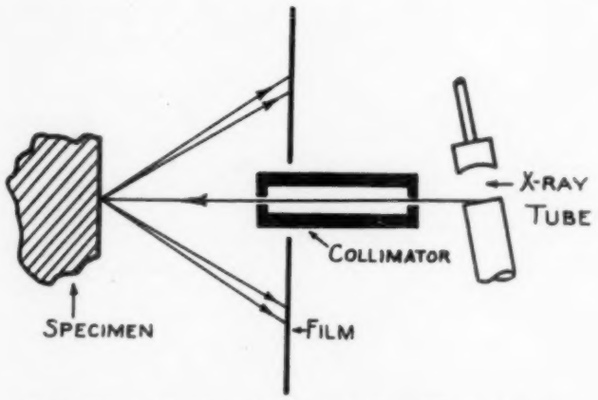


Fig. 1—Diagram of Experimental Arrangement of X-ray Diffraction Apparatus

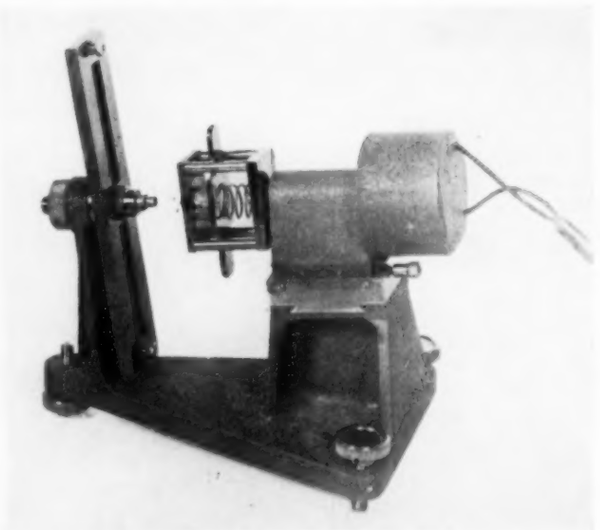


Fig. 2—An X-ray Diffraction Camera Used for Strain Measurement

diffracted beam, which is bent through an angle of nearly 180 degrees, is recorded on the film. Characteristic radiation from the X-ray tube is used which generally contains two wave lengths close together (the K-alpha doublet) so that the locus of all diffraction images which are to be studied will consist of two concentric circles close together on the film. Some modifications of the technique depend upon the examination of the images of individual grains. For this purpose, both specimen and film are stationary and usually the whole diffraction circle is recorded. Other modifications are based upon the average effects of a large number of images so that the sample must be rotated or moved in some other way so that as many grains in as many orientations as possible will be recorded. The illuminated spot on the sample, however, must be maintained at a constant distance from the film. The film also may be oscillated in its own plane. Under these conditions the diffraction circles are smooth and continuous so that it is only necessary to record a small portion on a narrow film. Figure 2 shows a laboratory type of camera in which the sample can be rotated and simultaneously translated in its own plane during the exposure. In the present work the following constants were used in the experimental arrangement:

Radiation—Cobalt K-alpha
Tube Current—10 ma.
Tube Voltage—40 kv.
Collimator—1 mm. holes—4 cm. apart
Sample to Film Distance—7 cm.
Film—Eastman Duplitized
Time of Exposure—1 hour

* To be presented at Annual Meeting, AMERICAN WELDING SOCIETY, Atlantic City, October 18–22. Contribution to the Fundamental Research Division, Welding Research Committee.

† Associate Prof. Dept. of Metallurgy, Mass. Institute of Technology.

[1] Norton, AMERICAN WELDING SOCIETY, JOURNAL, 11 (9), 5 (1932).

In order to understand properly the X-ray results, it is necessary to distinguish between elastic and plastic strain and also consider the general scale of the elastic strain. The strain measurement requires the use of a finite area of the specimen, in the case of X-rays about two to five square millimeters, and the result is a sort of average of the strains in this area. If the strain is uniform over this region so that it can be assigned a definite direction and magnitude, it is known as a homogeneous or macroscopic strain system. On the other hand, if the strain varies from point to point within this small area, it is known as a heterogeneous or microscopic strain system. In this latter case no definite quantitative value can be assigned to the strain but one can merely state that the range of strain is large or small. However, it is possible to compare two samples quantitatively on some arbitrary basis.

From a fundamental point of view, strains represent displacement of atoms from their normal positions in the metal crystal. In an unstrained crystal the arrangement is amazingly regular and the atoms recur with a definite periodicity. When a microscopic strain system exists, this regularity is broken up because the atoms are displaced by varying amounts from their normal positions. The crystals have become imperfect. A macroscopic strain also means atom displacement, but in this case the displacement is uniform and regular over a considerable region. The crystals are still perfect, in a sense, but are less symmetrical. If the strains, either macro or micro, are elastic, the displacement is always much less than the normal interatomic distance and a particular atom still has the same nearest neighbors although it may be closer to some and farther from others.

Plastic strain is primarily a translation of portions of the crystal along planes of atoms and may involve displacements of many times the normal interatomic distance. At the same time, during the gliding process, an atom on the slip plane is always close to other atoms although not its original neighbors. This possibility of interchange of atomic position without actual failure of the material is a characteristic of plastic crystals. In a polygrain material the mechanism of plastic flow is complex and the fragments of what was once a single grain receive slight rotations with respect to one another. At the same time the gliding action sets up a system of microscopic elastic strains in the vicinity of the slip planes. Thus, plastic strains are always accompanied by elastic strains but the converse is not necessarily true.

The mechanism of stress or strain relief is simply one of permitting the atoms to move back again into their normal positions. As the temperature is raised, the mobility of the atoms is increased and the residual crystal deformations are smoothed out. The degree to which these strains are removed is a function of time as well as temperature and is really a problem of diffusion. However, during recovery no single atom moves very far with respect to its neighbors. It is always less than the interatomic distance. It will be obvious, therefore, that there is no recovery from the plastic strain but only of the elastic strain accompanying it. An atom will take up its proper position with respect to its neighbors of the moment rather than its original neighbors. Only actual recrystallization can remove the evidences of plastic strains.

For the problem of stress relief, it is necessary to employ a technique which will show the existence of heterogeneous elastic strains. A treatment which will remove these will, at the same time, relieve the homogeneous strains which also may exist in a weld. The basis of a technique for doing this is somewhat analogous to the reflection of light by a mirror. If the mirror is truly plane, the reflected image will be sharp and undistorted. On the other hand, if the mirror has a rough or wavy surface, the image will be distorted or indistinct. The same is true for X-ray diffraction. If the crystal grain is perfect, its diffraction image will be sharp and clear, but if it is imperfect, that is, if it contains heterogeneous elastic strains, its image will be blurred or broadened out. The amount of blurring will be a measure of the range of strain in that particular grain.

Figure 3 shows the effect of grain size and grain perfection on the diffraction pattern with the sample stationary. Obviously the number of images appearing on the locus circles will be determined by the number of grains within the illuminated area. The sample of pattern (*a*) has a coarse grain, about two grains per square mm., that of (*b*) is medium size of perhaps 75 grains per square mm., while that of pattern (*c*) is a very fine grain sample. Pattern (*d*) is of a sample having the same grain size as (*a*) but the grains are distorted. The difference between (*a*) and (*d*) is very striking.

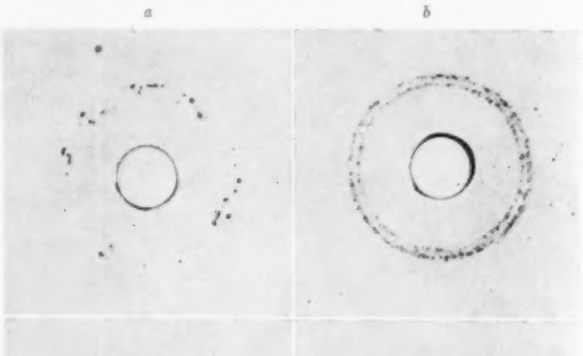


Fig. 3—Diffraction Photograms Showing Effect of Grain Size and Distortion
(a) Coarse Grain (c) Very fine Grain
(b) Medium Grain (d) Coarse Distorted Grains
(Sample Stationary During Exposure)

For many purposes, an examination of the diffraction images is sufficient to indicate the condition of strain in the specimen. But in other cases some numerical figure of the degree of strain is desired so that specimens can be compared more precisely. Many attempts have been made[2] to measure the peripheral or radial width of the diffraction images but the results have been rather unsatisfactory. A better scheme seems to be to take the photogram under such conditions that the complete locus circles are filled up. With a stationary sample this condition would be fulfilled only if the grain size were very small but it can be approximated by rotating the sample or combining rotation with translation to bring a large number of grains into the illuminated area. The photogram will then consist of smooth continuous circles whose width is a measure of the condition of strain in the sample.[3] With coarse-grained samples, this situation is not easy to attain and unless the diffraction circles are perfectly uniform, the line width measure-

[2] Regler, *Ergebnisse der Technischen Röntgenkunde*, 3, 183 Leipzig, (1933).
[3] Barrett, *Metals & Alloys*, V 171 (1934).

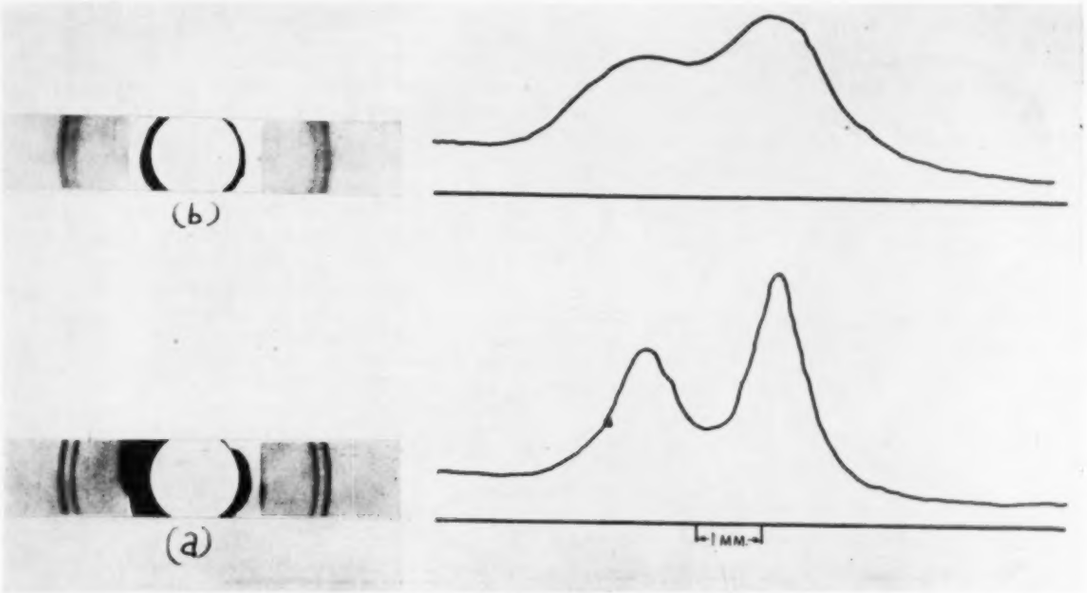


Fig. 4—Diffraction Photograms and Their Corresponding Densitometer Records
(a) Unstrained Grains
(b) Strained Grains
(Sample Rotated During Exposure)

ments are worthless. The weld specimens described later were of sufficiently fine grain so that only rotation of the specimen was necessary to give good diffraction circles.

With perfect grains the diffraction circles have a finite width determined by the size of the collimator and the wave-length distribution in the primary beam, but the lines broaden rapidly as the amount of elastic strain increases until the two circles merge into one broad band. The width of the lines is best measured by means of a micro-densitometer which makes a record of the blackening of the film as a function of the radial distance. In Fig. 4 are shown the photograms of two similar specimens, one with unstrained and the other with strained crystals. With each photogram is the corresponding densitometer record which shows the difference in a striking fashion. It is customary to measure the line width at a point which is one-half the maximum ordinate after subtracting the background density. The difficulty is in the overlapping of the two lines. Since it is desired to measure changes only, some convention may be adopted which best suits the particular case. In the present work, the ratio of the half width of the K-alpha one line to its maxim ordinate has given consistent results. Wood[4] has suggested a method of calculation based upon both lines of the doublet.

In order to illustrate the application of this method to welding, an examination was made of a simple butt weld in steel plate. The plate was of low-carbon steel one-half inch in thickness and a welding rod of similar composition was used. The weld was made by the electric arc process and was laid down in three passes.

For purposes of this examination the welded plate was cut up into narrow coupons and these specimens given a stress relief treatment for two hours at temperatures ranging from 400 to 650° C. (752 to 1202° F.) and slowly cooled. Each specimen was then polished on the section face, etched to remove the cold worked layer and five areas examined; the plate at some distance from the weld, the heat zone and the three passes in the weld itself. Both stationary and rotating sample techniques were employed and line broadness measurements were made from the rotation patterns.

[4] Wood, *Phil. Mag.*, **XV**, Ser. 7, 557 (1933).

Table 1 and Fig. 5 show the results on the base metal, the first pass and the final pass of the weld metal. The curve for the base metal has been drawn in and the other points shown in relation to it. These areas have had quite a different metallurgical history, but all show considerable strain in the as-welded condition and behave in much the same way during the process of strain relief. The base metal is hot rolled plate, probably finished at such a low temperature that it contains considerable residual strain. The first pass had originally a cast structure but due to the heat of subsequent passes it has been completely recrystallized and has a quite fine grain. In spite of having been heated to a high temperature, it is severely strained. The final pass has

Table 1—Variation of Line Breadth* with Stress-Relieving Temperature

	As-Welded	400° C.	450° C.	500° C.	550° C.	600° C.	650° C.
Plate	0.93	0.58	0.51	0.42	0.33	0.29	0.24
First pass	1.00	0.56	0.62	0.37	0.32	0.28	0.24
Final pass	0.80	0.57	0.57	0.51	0.37	0.38	0.31

* Note—Line breadth is the ratio of the half breadth to the height of the peak on the densitometer record and consequently is dimensionless.

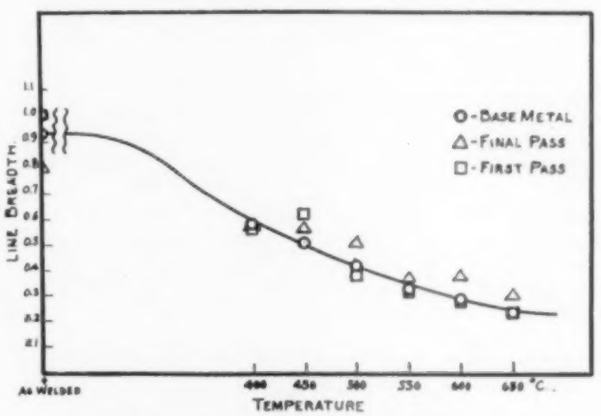


Fig. 5—Relation of Line Breadth and Stress-Relief Treatment for a Weld

a coarse grained cast structure and would be expected to be in a strained condition.

The stress-relieving treatment produces a marked sharpening of the diffraction lines even at temperatures as low as 400° C. (752° F.) indicating a removal of the microscopic elastic strains. Still lower temperatures should be investigated in order to have a complete picture of the situation. This sharpening continues up to the highest temperature used where actual recrystallization and grain growth are observed. Stationary pictures of these areas at 550° C. (1022° F.) show no evidence of recrystallization but the 600° C. (1112° F.) set shows definite indications. This is judged by the appearance of tiny sharp spots superimposed upon the continuous rings of the pattern. These spots are images of the new strain-free grains which have become large enough to give individual records. A stress-relieving treatment which produces recrystallization would probably soften the weld unduly. The final line breadth, however, is close to the theoretical condition given by perfect crystal grains.

It must be remembered that the line breadth values are purely arbitrary figures employed in comparing the different specimens and cannot be translated into pounds per square inch or any similar values because of the very nature of a microscopic strain system. However, one can be certain that the line breadth figures are related to the average level of strain in the specimen and that changes in line breadth are indications of changes in the state of strain.

Under present conditions the precision of the line breadth measurements is not high but is sufficient for most practical purposes. Broad lines can be measured with less precision than sharp lines but in general the results are reproducible within ten per cent. All values are averages of densitometer records made of the opposite sides of the same film and the agreement of these two records is very good.

Since the strain measurement is confined to the surface layers of metal, the preparation of the surface of the area to be examined is of the greatest importance. In general, a rough polish followed by a fairly deep macro-etch will prove satisfactory. It is essential that the etching should remove the layer of cold worked metal produced by the polishing operation, but at the same time it should not produce marked pitting. The effectiveness of the etching may be tested by taking a photogram and then etching a little more deeply and taking another. The line breadth should remain constant. On welded structures a small spot can be prepared in this way without injury to the structure itself.

One might ask whether there are not other causes than microscopic strains which will produce line broadening and there are two others which are recognized. One of these is a very small grain size of less than colloidal dimensions, and it is extremely doubtful that such grains exist in a weld. The second is a variation in composition of the phase which produces the diffraction pattern. This phase must be a solid solution and the interatomic distance of such a phase varies with the composition. Thus, if the composition varies within the area being examined, the lines will be broadened. The stress-relieving treatment, however, is at too low a temperature and lasts for too short a time to bring about any appreciable homogenization. Thus, any line-sharpening observed must be due to a removal of the micro strains. A final width greater than the theoretical minimum could be set down to nonuniformity of composition.

The practical use of this method in the welding field is now largely dependent upon the development of a really portable X-ray unit which is suitable for diffraction work. For this purpose, the tube should be able to provide an intense beam of comparatively long wave-length radiation and be fitted in a ray-proof and shock-proof mounting. Flexible insulated leads should connect the tube with the transformer equipment which in turn should be small and easily portable. The X-ray camera with a rotating film can be very simple and an integral part of the tube shield. No such equipment is built by American manufacturers at the present time, but it is made abroad and is finding ever-increasing use. Undoubtedly if the method can prove to be of real value, the proper equipment will be developed here.

For a considerable proportion of the work in which this method of strain measurement would be used, a micro-densitometer record would not be needed. The eye is very sensitive to changes in the sharpness of the lines and a series of photograms can be classified in this way with a surprising degree of accuracy. However, with a proper densitometer, of which there are several types available, records can be run off in a few minutes and measurements from these records give quantitative results for comparison.

In the meantime, there is a great deal which can be done in the laboratory with standard X-ray diffraction apparatus. In the first place, it should be possible to relate the measured width of the diffraction image to some definite condition in the metal. It has been shown[5] that when certain metals are strained by cold working, there is a limit to the broadening of the line and further working does not increase it. The suggestion has been made that this maximum represents the greatest amount of elastic distortion which the crystal can exhibit. If this is the case and a definite limit can be shown to be characteristic of each metal, then it might be possible to express the state of strain as some fraction of the maximum.

Then there is the important problem of relating the stress relieving as indicated by the X-ray photograms with the mechanical properties of the welded joint. Much useful information could be obtained in this way which could be applied directly to the problem of design. Such an investigation, including work on many of the typical steels used for welded construction, would be of considerable value.

The X-ray diffraction method of indicating the degree of distortion of metal crystals has been described and its application to the study of the stress-relief process in welds has been illustrated by a typical example of a welded joint. It has been shown that a change in the width of the X-ray lines is related to the changes which take place during the stress-relief treatment. The X-ray evidence is related to the state of the microscopic strain system at a particular point, and is perhaps more direct evidence than can be obtained in other ways. It is believed that the behavior of the micro strains during the relieving operation is similar to that of the macro strains in the same region. Considerable development is necessary in apparatus, technique and interpretation if the method is to be used practically, but the importance of the results justifies further investigation in this field.

[5] Wood, *Phil. Mag.*, **XIV**, Ser. 7, 656 (1932).

Fatigue Tests of Butt Welds in Structural Plates*

By W. M. WILSON†

THE object of the tests described in this paper was to determine the fatigue strength of butt welds in $^3/_4$-in. steel plates of structural grade. The term *fatigue strength* of a weld as used herewith has been arbitrarily taken as the maximum stress to which a weld can be subjected 2,000,000 times without failure. The specimens were subjected to a concentric axial force and they were tested on cycles in which the stress varied from zero to a maximum tension. The general dimensions of the specimens are given in Fig. 1.

The machine used for the test is shown in Fig. 2. It was originally designed and constructed for use in an extensive series of fatigue tests of riveted joints that has been conducted by the Engineering Experiment Station, University of Illinois, in cooperation with the Department of Public Works, State of California. The machine is designed to produce an axial force in the specimen and to operate on a stress cycle in which the axial force may vary from 200,000 lb. tension to 200,000 lb. compression (−200,000 lb. tension). Moreover, either the maximum or minimum stress can be varied independently of the other so as to produce any desired cycle including a complete reversal, a partial reversal or a pulsating stress having any desired relation between the minimum and maximum stresses. The only cycle that was used in the fatigue tests of the butt welds, however, is one in which the axial stress varied from zero to a maximum tension.

The essential features of the machine are shown in the photograph of Fig. 2. The force that produces the stress in the specimen originates in the variable-throw eccentric and is measured by the dynamometer. This force is multiplied by the I-beam lever, which has a multiplication ratio of 18. The specimen is bolted to the pulling heads as indicated in the foreground. The I-beam lever is supported on bearings which provided for the free angular motion caused by the up-and-down movement of the outer end of the beam produced by the eccentric.

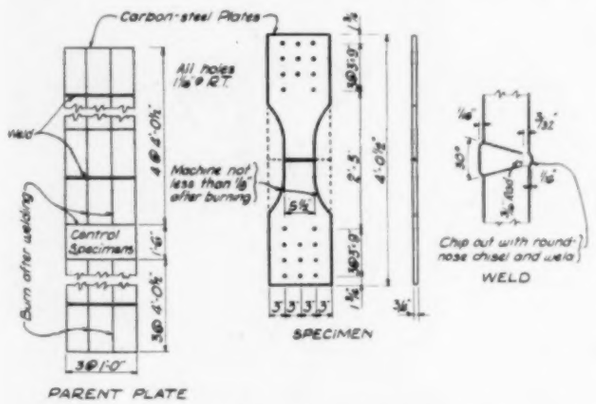


Fig. 1—Specimens for Fatigue Tests of Butt Welds in Carbon-Steel Plates

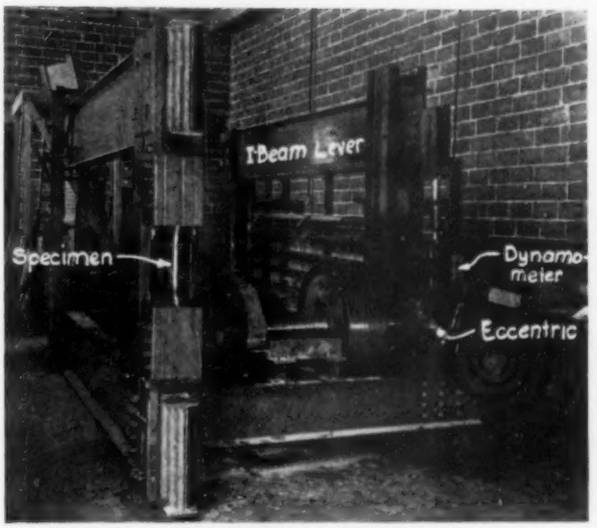


Fig. 2—Duplex Fatigue Machine

Similar bearings are provided at the outer ends of both pulling heads so as to give the specimen freedom from angular restraint.

The procedure for beginning a test is as follows: The I-beam lever is placed in a horizontal position and the specimen is bolted to the pulling heads, care being taken to have the specimen and the two pulling heads in proper alignment. After the bolts have all been tightened, the throw of the eccentric is adjusted to give the desired stress range, and the length of the connecting rod between the eccentric and the outer end of the I-beam lever is adjusted to give the proper relation between the maximum and the minimum stress. Both of these are cut-and-try processes and the machine is cranked by hand while the adjustments are being made. The total force on the specimen to be used during a test is determined from the unit stress to be used and the dimensions of the particular specimen being tested. The corresponding deflection of the dynamometer is computed from the total force on the specimen, the multiplication ratio of the lever and the calibration constant for the dynamometer. With the required deflection of the dynamometer known, the machine is turned by hand and the stress range and the ratio of the minimum to the maximum stress are noted and compared with the desired values. If the two sets of values do not agree, and they seldom do on the first trial, the throw of the eccentric and the length of the connecting rod are changed and the dynamometer readings are again noted. This process is repeated until the desired dynamometer readings are obtained. With a little practice the operator becomes quite proficient in making these adjustments and the process does not prove to be as tedious as its description indicates.

The machine was operated at a speed of 180 r.p.m. and ran day and night without an attendant. The load

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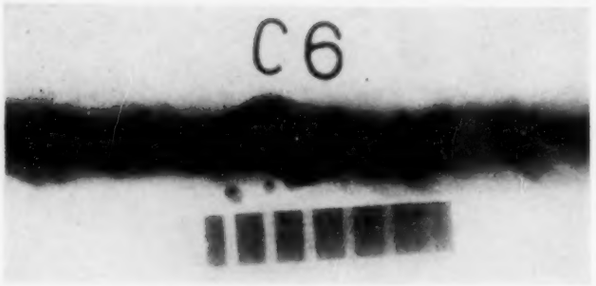

Fig. 3—Radiograph of the Weld in C6

was checked and necessary readjustments were made twice each day. Usually, except for a few hours at the beginning of a test, the load remained very constant. The method of measuring the load is not precise but it is believed that in general the indicated load on the specimen was accurate within 1 or 2 per cent.

The specimens were made up of a number of groups, each group consisting of three identical specimens. In making the tests of the three specimens of a group, the stresses were so chosen that, as nearly as could be estimated in advance, one specimen would fail at approximately 500,000 cycles, one at 1,000,000 cycles and one at 2,000,000 cycles. If all of the tests of a group were consistent, the three points representing the results of the three tests would fall on a smooth curve and the point of intersection of this curve with the 2,000,000-cycle ordinate would represent the fatigue strength. Unfortunately the three tests of a group were not usually consistent and the three points representing the three tests of a group did not determine a smooth curve, and some other means appeared desirable in the interpretation of the results.

Extensive test of small carefully machined specimens indicate that the left-hand portion of the *S-N* curve (Stress-number-of-cycles-for-failure curve) can be represented by the empirical equation[1] $S = \frac{B}{N^K}$, in which S is the maximum stress in the stress cycle, N is the number of cycles for failure and B and K are experimental constants. Adopting the same equation for the *S-N* curve for the test of welds, the ordinate of the point in which this line intersects the 2,000,000-cycle ordinate is, by definition, the fatigue strength of the member tested. Letting this fatigue strength be represented by F, its value, as determined by a single test, is given by the equation $F = \frac{SN^K}{2{,}000{,}000^K}$. The experimental constant K in this equation should be such that the value of F is not affected by the unit stress used in a test. Since the equation is empirical and the tests of a group are not consistent, this requirement cannot be complied with exactly. Instead, a value was selected by a cut-and-try method that most nearly complied with this requirement, considering all of the groups of tests of the investigation. The value of K thus determined, 0.10, was used in the above expression for F in determining the fatigue strength from the results of individual tests for all specimens except the small machined specimens reported in Table 6.

The three tests of a group are not always consistent with each other and no method of interpreting the results can eliminate these inconsistencies. Moreover, the character of the "stress raisers" that exist in all fatigue specimens probably affects the value of K, so that the true value of the latter is somewhat different for different specimens. Nevertheless, it is believed that the method described is fairly satisfactory in obtaining from each test an approximate value of the fatigue strength for the particular specimen. The average of the individual values obtained from the three tests of a group is taken as the fatigue strength for the group.

Nine groups of three specimens each were tested. For seven groups the specimens are butt-weld joints of $^3/_4$-in. carbon-steel plates and for two groups they are butt-weld joints of $^3/_4$-in. silicon-steel plates. The chemical composition and the physical properties of the plates as given by static tests of standard control specimens are given in Table 1.

The details of the welds and the location of the plates in the parent plate are given in Fig. 1 and Table 2 for the specimens with carbon-steel plates. Although the three specimens of a group were made as near alike as possible, each of the seven groups with carbon-steel plates had some feature that was characteristic of that group alone. These characteristics are given in columns 1 and 2 of Table 3. The first five groups were hand welded with a metallic arc, using a shielded-arc electrode. For the first group the weld metal was planed flush with the base plate on both sides; for the second group the specimens were tested in the "as-welded" condition; for the third group the specimens were stress relieved by heat treatment; the fourth group contained purposely poor welds; and for the fifth group each bead was peened before the following bead was laid. All specimens except those of the fourth group were welded by the same Class A welder; those of the fourth group were welded by a less

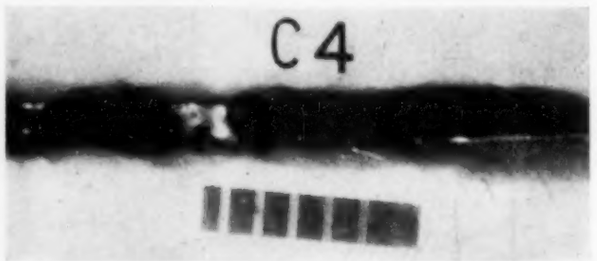

Fig. 4—Radiograph of the Weld in C4, a Purposely Poor Weld

[1] See *Bulletin* 124, p. 92, University of Illinois, Engineering Experiment Station.

Table 1—Chemical Composition and Physical Properties of Plate Material

Kind of Plate	Chemical Composition C	Si	Mn	S	P	Physical Properties Strength, Lb. per Sq. In. Ultimate	Yield Point	Elongation in 8 In. Per Cent	Reduction of Area Per Cent
Carbon steel	0.15	0.061	0.497	0.022	0.024	54,500	30,960	34.2	60.6
Silicon steel	0.30	0.23	0.91	0.027	0.022	80,700	49,800	25.0	57.4

experienced welder. Radiographs showed that, except for group four, all welds were free from all except very small flaws. The radiograph of C6, which is reproduced as Fig. 3, shows two small blow-holes in the weld. These are as large as any of the flaws in other than the purposely poor welds. The radiograph of C4, a purposely poor weld, reproduced as Fig. 4, shows large slag inclusions.

Table 2—Welding Data. Specimens with Carbon-Steel Plates

Bead No.	Diam. of Rod, In.	Amperes	Volts
1	3/16	180	28
2	3/16	190	28
3	1/4	340	33
4	5/16	430	35
5	5/16	380	30
6	1/4	360	35

Of the specimens welded with the automatic carbon arc, those of the first group were stress relieved by heat treatment and those of the second group were tested in the "as-welded" condition.

The results of the tests of welds in the carbon-steel plates are shown in Table 3. For the specimens hand welded with a metallic arc, the fatigue strength of the specimens in the "as-welded" condition is 21,800 lb. per sq. in., and all specimens broke at the edge of the weld where there is a sudden change in section. Contrasted with this, the specimens for which the weld metal was planed flush with the base plate on both sides had a fatigue strength of 27,900 lb. per sq. in., and all specimens broke outside of the weld. The fatigue strength of the other three groups had values as follows: Specimens stress relieved by heat treatment, 23,200 lb. per sq. in.; specimens having each bead peened, 22,300 lb. per sq. in.; and specimens with purposely poor welds, 20,600 lb. per sq. in. The number of tests available is not great enough to justify any final conclusions but it is, nevertheless, of interest to note the relative standing of the various groups. Taking the fatigue strength of the group of specimens in the "as-welded" condition as 100, the rating of the other groups is as follows: Weld metal planed flush with base plate on both sides 128, stress relieved by heat treatment 106, each bead peened 102 and purposely poor welds 94. Planing off the excess weld metal so as to eliminate the sudden change in section appears to have had a greater effect on the fatigue strength than stress relieving by heat treatment or than peening the beads. It is also of interest to note that the fatigue strength of the purposely poor welds is only 6 per cent less than the fatigue strength of the good welds in the "as-welded" condition.

The fatigue strength of the welds in the carbon-steel plates that were welded with an automatic carbon arc was 22,800 lb. per sq. in. for the specimens in the "as-welded" condition and 25,800 lb. per sq. in. for the specimens stress relieved by heat treatment. These, rated on the same basis as that used in the previous paragraph, have a rating of 105 and 118. The average strength is a little greater for the specimens welded with an automatic carbon arc than it is for those hand welded with a metallic arc but the difference is not great. The lowest individual value for the fatigue strength of the 18 welds in carbon-steel plates (not including the welds that are purposely poor) is 20,400 lb. per sq. in. for B6.

The details of the welds and the location of the plates in the parent plate are given in Fig. 5 and Table 4 for the specimens with silicon-steel plates. All specimens were hand welded with a metallic arc. The first group was tested in the "as-welded" condition but for the specimens of the second group the weld metal was planed flush with

Table 3—Fatigue Strength of Butt Welds in 3/4-In. Carbon-Steel Plates

King of Weld	Finish of Weld	Spec. No.	Stress Cycle in 1000 Lb. per Sq. In.	Cycles for Failure in 1000's	Fatigue Strength Lb. per Sq. In.*	Remarks
Hand welded Shielded-arc Metallic arc electrode	Weld planed flush with base plate on both sides	A7	0 to 30.0	650	26,900	Broke 1 in. from weld.
		B7	0 to 32.0	904	29,600	Broke 3 in. above weld and 1 in. below weld.
		C7	0 to 28.0	1538	27,300	Broke 1.5 in. from weld.
		Av.			27,900	
	As-welded	A6	0 to 26.0	300	21,600	Broke at edge of weld.
		B6	0 to 22.0	927	20,400	Broke at edge of weld.
		C6	0 to 28.0	305	23,300	Broke at edge of weld.
		Av.			21,800	
	Stress relieved by heat treatment	A5	0 to 23.0	1030	21,600	Broke at edge of weld.
		B5	0 to 26.0	633	23,200	Broke at edge of weld.
		C5	0 to 28.0	609	24,900	Broke at edge of weld and 2 in. from weld.
		Av.			23,200	
	Purposely poor welds	A4	0 to 26.0	503	22,700	Broke at edge of weld.
		B4	0 to 23.0	601	20,400	Broke at edge of weld.
		C4	0 to 20.0	944	18,600	Broke at edge of weld.
		Av.			20,600	
	Each bead peened	A3	0 to 30.0	213	24,000	Broke at edge of weld.
		B3	0 to 24.0	900	22,200	Broke at edge of weld.
		C3	0 to 20.0	2727	20,700	Broke at edge of weld.
		Av.			22,300	
Automatic carbon arc	Stress relieved by heat treatment	A2	0 to 28.0	1864	27,900	Broke in head. Weld O.K.
		B2	0 to 32.0	211	25,600	Broke at edge of weld.
		C2	0 to 30.0	194	23,800	Broke at edge of weld.
		Av.			25,800	
	As-welded	A1	0 to 30.0	220	24,100	Broke at edge of weld.
		B1	0 to 27.0	448	23,300	Broke at edge of weld.
		C1	0 to 22.0	1201	21,000	Broke at edge of weld.
		Av.			22,800	

* As computed from the equation $F = \frac{SN^{0.10}}{2{,}000{,}000^{0.10}}$

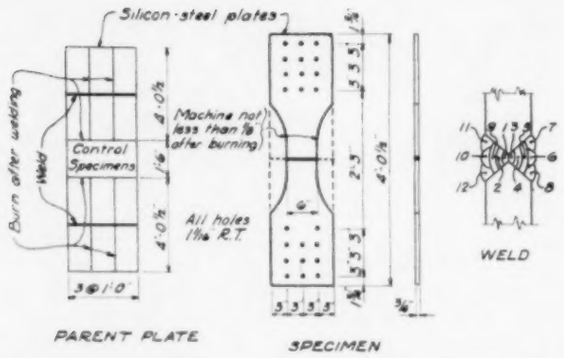

Fig. 5—Specimens for Fatigue Tests of Butt Welds in Silicon-Steel Plates

Table 4—Welding Data. Specimens With Silicon-Steel Plates

Bead No.	Diam. of Rod, In.	Amperes
1	5/32	160
2	3/16	230
Turned plate and chipped out underside.		
3 to 12 inclusive	3/16	230
Preparation of edge of plate:		
Flame cut 40 degree double bevel with radiograph.		
Ground bevel surfaces to clean.		
Set-up plates with 1/8-in. opening.		
Pre-heated to approximately 175° F.		
Welded with reversed polarity.		

the base plate on both sides. The results of the tests are given in Table 5. The fatigue strength of the two groups, the average of three tests in each instance, is 24,000 lb. per sq. in. for the first group, and 23,700 lb. per sq. in. for the second group. The fatigue strength appears to be less for the weld metal than for the base plate as evidenced by the fact that specimen 2 broke through the middle of the weld where the section was much larger than the section of the base plate outside of the weld. All of the specimens of the second group broke at the center of the weld precluding any strengthening effect that might result from planing off the excess weld metal. The strength of the welds was very nearly the same for silicon-steel plates as it was for the carbon-steel plates of Table 3.

Geometrical imperfections are known to be stress raisers that affect the fatigue strength of metal parts. For this reason the fatigue strength of a material depends upon the geometrical properties of the specimen. The fatigue strength of structural steel as determined by various types of specimens is given in Table 6.[2] The upper part of the table is for carbon steel and the lower part is for silicon steel. The group of purposely poor welds was not included in the average strength of welds in this table. It is of interest to note that the fatigue strength of the welds is about 88 per cent of the fatigue strength of the plates of riveted joints, the latter being

[2] The data for all except the welded specimens are taken from an unpublished report of tests of riveted joints made by the University of Illinois, Engineering Experiment Station in cooperation with the Department of Public Works, State of California. The tests of the round machined and polished specimens were made by Professor H. F. Moore.

Table 5—Fatigue Strength of Butt Welds in 3/4-In. Silicon-Steel Plates

Kind of Weld	Finish of Weld	Spec. No.	Stress Cycle in 1000 Lb. per Sq. In.	Cycles for Failure in 1000's	Fatigue Strength Lb. per Sq. In.	Remarks
Hand welded metallic arc electrode	As-welded	1	0 to 27.0	353	22,800	Crack began at edge of weld and extended into base plate.
		2	0 to 24.0	3015	25,100	Broke at center of weld.
		3	0 to 26.0	872	24,000	Broke at edge of weld.
		Av.			24,000	
	Weld planed flush with base plate on both sides	4	0 to 33.0	284	27,200	Broke at center of weld.
		5	0 to 30.0	41	20,400	Broke at center of weld.
		6	0 to 27.0	465	23,400	Broke at center of weld.
		Av.			23,700	

Table 6—Fatigue Strength of Structural Steel as Determined by Tests of Various Types of Specimens

Type of Specimen	Strength, Lb. per Sq. In. Static	Strength, Lb. per Sq. In. Fatigue*	Fatigue Strength / Static Strength	Fatigue Strength / Fatigue Strength of Polished Specimen
Carbon Steel				
Round, machined and polished	64,700	47,000	0.73	1.00
Plate with mill scale	61,800	30,300	0.49	0.67
Plate with mill scale and 1 1/8-in. drilled hole	61,800	21,200	0.34	0.47
Plates of riveted joints	63,250	27,130 (44 tests)	0.43	0.59
Butt weld in 3/4-in. plate	54,500	24,000 (18 tests)	0.43	0.59
Silicon Steel				
Round, machined and polished	81,700	56,000	0.69	1.00
Plate with mill scale	80,800	35,800	0.44	0.64
Plate with mill scale and 1 1/8-in. drilled hole	80,800	23,900	0.30	0.43
Plates of riveted joints	88,050	26,680 (15 tests)	0.30	0.44
Butt weld in 3/4-in. plate	80,700	23,850 (6 tests)	0.30	0.43

* Stress cycle: Zero to maximum tension for all specimens.

based on the net section of the plate through the rivet holes. Moreover, this relation is practically the same for carbon-steel and silicon-steel plates.

The number of tests made in connection with this investigation is not great enough to justify final conclusions relative to the fatigue strength of butt welds in structural plates, and such tendencies as are indicated should be verified by additional tests. The tests reported in Tables 3, 5 and 6, however, seem to indicate:

1. The unit fatigue strength of the joints in carbon-steel plates in the "as-welded" condition was reduced by the stress concentration at the edge of the weld due to the change in section and it can be increased by planing off excess weld metal.

2. The fatigue strength of welds of carbon-steel plates was slightly greater for specimens welded with the automatic carbon arc than it was for those welded with the hand-operated metallic arc.

3. The fatigue strength of the welds in the silicon-steel plates was limited to that of the filler metal.

4. Peening the beads had practically no effect and stress relieving by heat treatment had but little effect upon the fatigue strength of the welds.

5. The unit fatigue strength of a properly made butt weld in the "as-welded" condition was about 88 per cent of the fatigue strength of the same plate in a riveted joint, the former being computed on the basis of the gross and the latter on the basis of the net section of the plate. This statement is true for both carbon-steel and silicon-steel plates.

Acknowledgments

The carbon-steel specimens were contributed by the Chicago Bridge and Iron Company and the silicon-steel specimens by the Bethlehem Steel Company. The tests were made by Frank P. Thomas and John V. Coombe, special research Assistants in Civil Engineering at the University of Illinois.

Welded Girders with Inclined Stiffeners*

By CYRIL D. JENSEN† and WM. F. LOTZ, JR.‡

Introduction

THIS paper is the first report of an investigation being made to determine the feasibility and the practicability of placing girder stiffeners on an inclination to the vertical. Now that welding is available as a tool for fabrication there is a definite possibility that inclined stiffeners can be detailed and placed as economically as vertical ones. Moreover, it will be shown in this paper that by so placing the stiffeners a considerable gain in strength of girder is obtained.

Six years ago two model girders were made of cardboard and subjected to load. The girder having inclined stiffeners was so much stronger than the one with vertical stiffeners that the problem appeared worthy of more research. The actual work began in 1934 with a thesis by Wm. Allen Robinson, a student at Lehigh University, who had time but for a polarized-light study of "Stress Distribution in Thin Webs of Girders." The work was continued in 1936 by Wm. Lotz, who made and tested two steel girders (see Fig. 1).

Notes on Design of the Girders

In order to keep the size of the steel girders as small as possible and yet use the limits set by Robinson in his work, a web material of $^1/_8$ in. structural grade steel was chosen. Using a depth to thickness ratio of 102 to 1, the same ratio as used by Robinson in the construction of his celluloid girders, a web-depth of 12.75 inches was obtained. Likewise the length of girders between supports was found to be 6 feet 2 inches by using approximately a 6 to 1 ratio of length to depth. This choice was made with the assumption that we were designing a short member which was to be used to support a heavy concentrated load at its mid-point. The design load of 31,880 lb. was found by multiplying the web area (12.75 $\times$ 0.125 = 1.595 square inches) by the allowable stress in shear (10,000 psi). The flanges were overdesigned by 167% in order to insure web failure. Each flange consisted of two plates—one, 6 in. x $^1/_4$ in. and one, 7 in. x $^1/_2$ in. The use of two plates was to facilitate the welding of the web to the flange. If this had not been done there was the definite possibility of burning the $^1/_8$-in. web material before the comparatively thick flange material was hot enough to alloy with the weld metal. The fillet welds measured about $^3/_{16}$ of an inch per leg. The inclined stiffeners were designed with the assumption that the girders would act as trusses. The four center stiffeners were assumed to carry their components of the load as direct stresses. The web was then assumed to carry the tension to the top of the next set of stiffeners and so on to the support points. The direct stress under working load in each inclined stiffener was calculated to be 7970 lb., and from this it was computed, by using an allowable stress of 12,000 psi that the required area was 0.768 sq. in. However, since the area required for the vertical stiffeners was a little less and since it was desired to use one size of stiffener for the two girders, $2^3/_4$ x $^1/_4$-inch plates were adopted giving an area of 0.69 square inch. It was assumed in the case of the inclined stiffeners that the web could be depended on to carry some of the stress, thus making up the deficiency in area.

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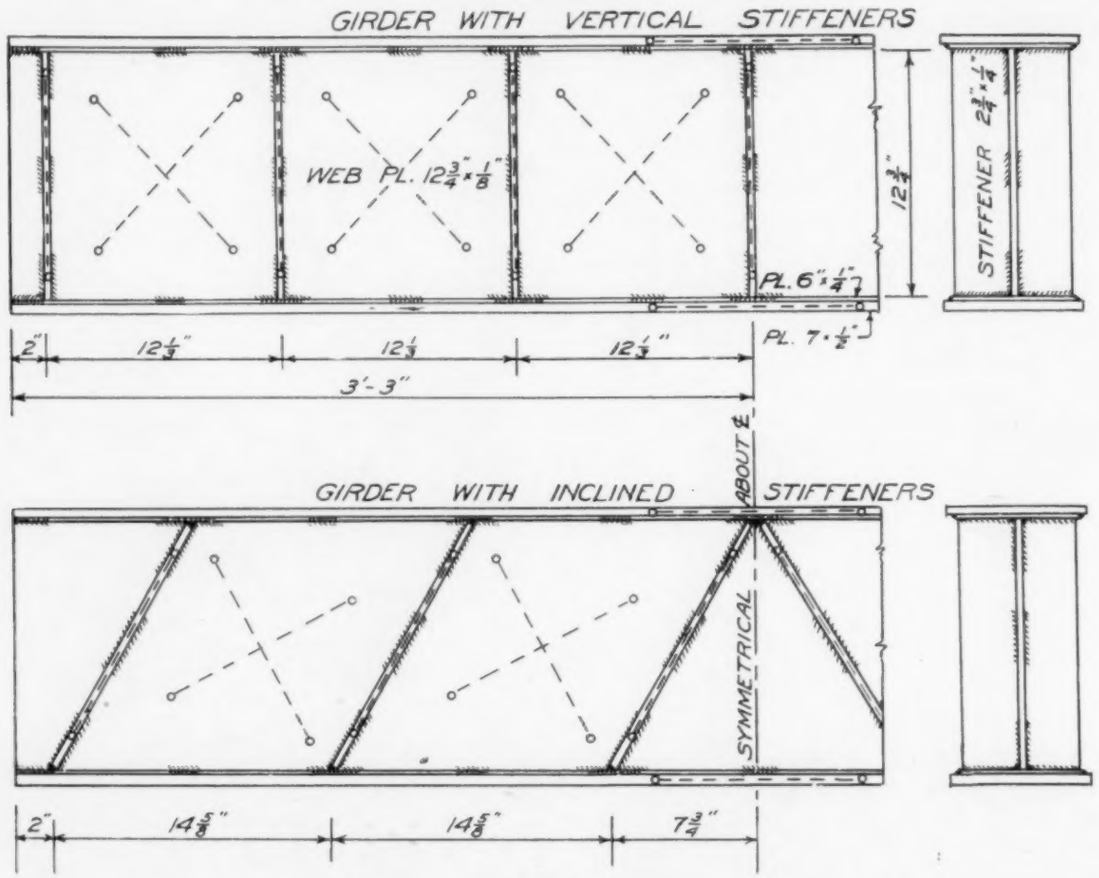


Fig. 1

The stiffeners were fused to the web and flanges with enough length of fillet welds to develop the full strength of the stiffeners at each end. In addition one tack weld was provided at mid-height between the web and each stiffener.

The total length of stiffening material required for the girder with inclined stiffeners was 177.5 inches (for six pairs of stiffeners). For the other girder seven pairs of vertical stiffeners were used on 12.32 inch centers, giving a total length practically identical with that of the first.

Results

It is not the intention in this brief paper to give all of the details of the investigation, which include the testing of tensile coupons cut from the webs of the girders, the exploration of stresses in the webs of the girders with Huggenberger tensometers and the testing of the girders before the stiffeners were placed. The summary of the unit stresses measured at one of the test loads, Table I, gives in condensed form the essential information obtained from the tests.

Stiffener Stresses.—It will be noted that all stiffeners in the second girder (the one with inclined stiffeners) have been put to work and that the stiffeners in the first girder carry practically no load, the exception being the pair under the load. It should also be noted that more research is needed in determining the most favorable inclination of stiffeners, since the inclined stiffeners are carrying only about half load.

Flange Stresses.—Maximum flange stresses in the second girder were consistently higher than in the first one. More investigation is needed to verify this point.

Web Stresses.—In the first girder the tensile and compressive stresses were approximately equal as was expected. In the second girder the tensile stresses were greater than the compressive stresses. A comparison between girders can hardly be made since (see Fig. 1) the gage line in the two girders differed in their directions. However, if we may theorize a little in the matter of *reserve strength* we should like to point out that it may be more favorable to have the tensile stresses exceed the compressive stresses in the web. If the stresses were the same, then, at yield point load the web would begin buckling in the direction of the compressive stresses, whereas, in the tensile direction the web would still have a good deal of reserve strength. If on the other hand buckling is delayed, greater loads can be carried.

Web Buckling.—The first signs of buckling in the girder with vertical stiffeners were noticed at about

Table 1—Unit Stresses (in Psi.) at Load 33,000 Lb.

		Girder with: Vertical Stiffeners	Inclined Stiffeners
Stiffener Stresses	*A*	−1350	−5250
	B	−1800	−5250
	C	−1350	−6450
	D	−5100	
Flange Stresses At midspan	Top	−6300	−9300
	Bottom	8400	9000
Web Stresses See Fig. 1	Panel 1	21,500 and −15,300	20,700 and −17,400
	" 2	15,600 " −13,200	19,650 " −13,500
	" 3	15,300 " −14,100	

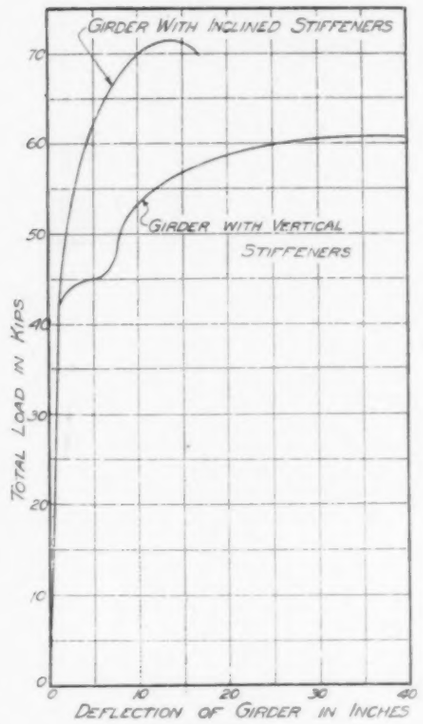


Fig. 2

44,500 pound as compared with 60,000 pound for the girder with inclined stiffeners. Pronounced buckling was noted at loads of 46,000 and 63,000 pounds for the two girders, respectively.

Comparative Deflections.—Load-deflection curves are given in Fig. 2 and tell the story more vividly than can be told by words.

Fabrication.—There appear to be no difficulties in the fabrication of the girders with inclined stiffeners. In fact a reasonable tolerance in length of stiffener would be permitted where such would not be the case for tight-fitting vertical stiffeners.

Application.—In looking at this research from the practical standpoint we see a chance for a variety of applications among which are deck-girder bridges, possibly through-girder bridges and certain girders which span openings in buildings and which carry one or more columns.

Conclusions

In conclusion we wish to state that this investigation is but the first step in a larger project. It has been proved that putting the stiffeners to work by inclining them in the direction of the compressive stresses in the web of the girder increases materially the strength of the girder. What still remains to be determined is the best angle of inclination for the stiffeners, and a suitable method for design of this new type of girder.

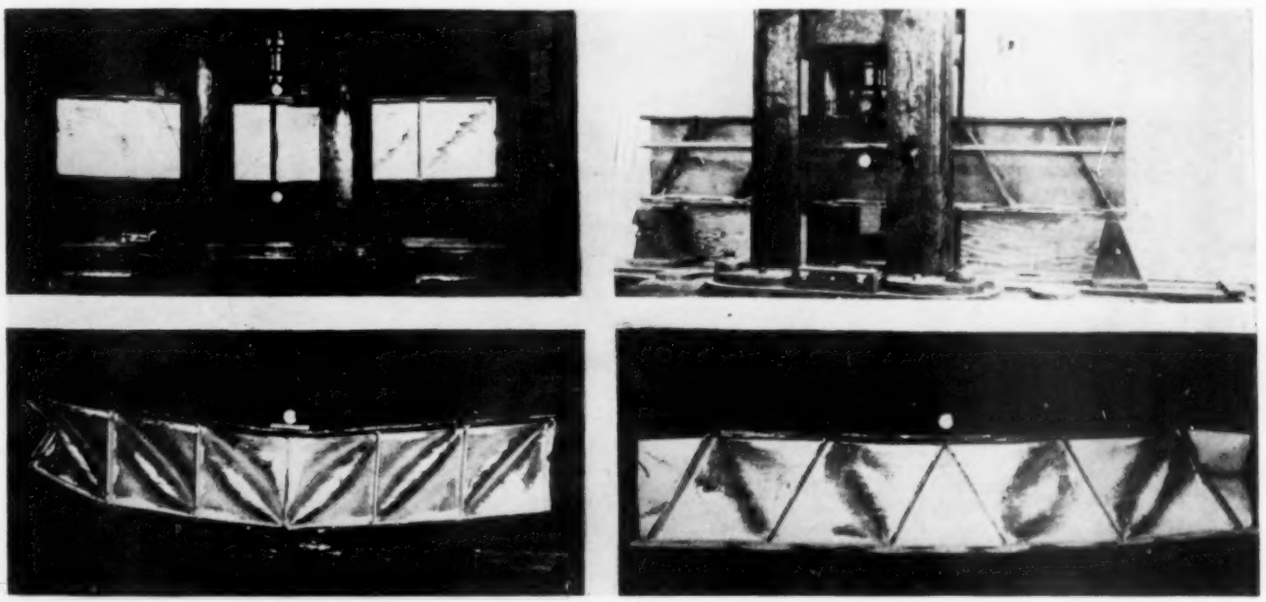


Fig. 3

Welding of Copper*

By A. P. YOUNG†

THE Welding of Copper as a research project was undertaken at the Michigan College of Mining and Technology in 1932–33, when it was found that very little literature had been published on that subject. This article is a report of the results of that research, carried on during the summer term of 1933, '34, '35, '36 and '37 and during the college years 1934–35 and 1936–37.

In this article will be presented: a review of the literature on the Welding of Copper condensed into tabular form; a short discussion of methods of obtaining and recording data, testing of welds and preparation of the results from both the long carbon-arc process and the oxyacetylene process of welding; photos and photomicrographs of welds, with explanations; and photos and curves showing a relationship between the hardness of the plates and tensile strength and ductility.

Before any welding on copper plate could be done at this college it was necessary to make a review of the literature in order to establish the best methods known to industry and technology. This review was made previous to and during the year 1934–35; and, although other literature has been published since which amplifies that previously presented, the table given with this article condenses the essential points of procedure in preparing and welding copper plate by three methods of welding. (See Table 1.) Since the review by the writer

* To be presented at Annual Meeting, American Welding Society, Atlantic City, October 18–22, 1937. Contribution to Fundamental Research Division of Welding Research Committee.

† Michigan College of Mining & Technology.

Table 1—Summary of Procedure for the Welding of Copper
As Applied to Butt Joints, Flat Welding

	Long Carbon-Arc Process	Metallic-Arc Process	Oxyacetylene Process
Preparing the plate	Bevel edges of plates thicker than 1/8 in. to form 60 to 90 "V" (4, 7).	(4) and (2) do not recommend this process for copper. Bevel the same.	Same as for carbon arc (10).
	For plates 1/4 in. and thicker a double "V" may be advisable (4).	Same as for carbon arc.	Same as for carbon arc.
Cleaning	Welding edges must be free of all foreign matter (3, 4).	Same as for carbon arc (4).	Same as for carbon arc (5).
Spacing	Edges should be spaced 1/16 in. to 1/8 in. at bottom of "V" to allow penetration of filler metal (4).	Same as for carbon arc (2).	Edges should be spaced 1/16 in. to thickness of plate at start of weld, increasing distance toward end (10).
Clamping	The plates should be securely clamped in place to prevent warping (4).	Same as for carbon arc (4).	Same as for carbon arc.
Diameter of rod	Diameter of rod equal to or 20% greater than thickness of plate (4). (Table 3, App.)	Same as for carbon arc. See Appendix, Part I.	Same as for carbon arc. See Table 2 of appendix.
Flux	90% borax and 10% sodium fluoride (2) or any good commercial flux (5). If special rod is used no flux required.	Same as for carbon arc except flux aids when special rod is used.	Any good brazing flux (5). For deoxidized copper both rod and plate to receive application (10, 6). Special rods require flux.
Backing bars	Backing bars of copper, cast iron, steel or carbon to be used to back up plates (4, 3).	Same as for carbon arc.	Same as for carbon arc. Can be clamped to iron table or bench.
Back bead	Made possible by grooving backing bars or raising plate with saw blades (8, 4, 3).	Same as for carbon arc.	Same as for carbon arc. When not possible, butt edges 1/16 in., lay full bead (8).
Polarity	Normal: work positive, electrode negative (4).	Reversed: work negative, electrode positive (2).	
Electrode or tip	Carbon: to be sharpened to long pencil point and gripped less than 3 in. from tip (4). See appendix, Table 3.	Metallic: special rods or deoxidized copper with flux coating (2).	Welding tip to be two sizes larger than tip used for same gage steel plate (5, 7). See Appendix, Table 2.
Voltage or pressure	40 to 60 volts as required. See appendix for current requirements (1, 3, 4).	20 to 30 volts (4).	Oxygen and acetylene pressure to correspond to make and size of tip (9).
Direction of flame	In a plane perpendicular to plate, inclined slightly from vertical (4).	Same as for carbon arc.	In a plane perpendicular to and directed downward onto both edges of plate (6).
Direction of welding	Forehand: i.e., right to left. See Fig. 2 (4).	Backhand: i.e., left to right.	Backhand or forehand (10).
Flame	Arc to be held from 1 in. to 1 1/4 in. long (3, 4).	1/4 in. to 1/2 in.	Neutral and soft, not short with strong blast (5, 6, 7, 9, 10).
Position of rod	Filler rod held about 1/4 in. above work and parallel to groove, arc impinging onto rod so held (4).	Shown under direction of flame.	To suit welder.
Speed of welding	12 to 24 inches per minute. Best bead laid at fast (1, 3) speed and high current (4, 8).	Determined by depth of penetration and laying full bead in one pass.	Speed should be regulated to condition of heat for laying of full bead in one pass.

(4), etc. For source of information see Bibliography at end of article.

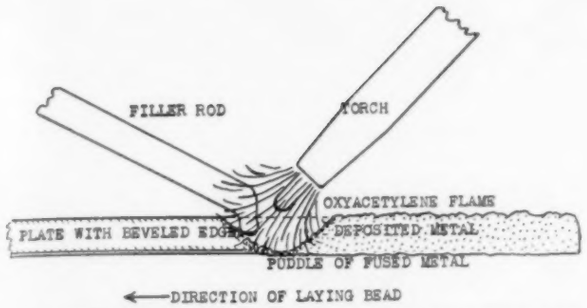


Fig. 1—Detail Showing Relative Position of Tip and Filler Rod During Welding

a more exhaustive review has been made and presented to you by I. T. Hook of the American Brass Company in a very comprehensive and interesting series published in the February and March issues of THE WELDING JOURNAL, **16,** 1937. Table 1, condensing as it does the information obtained from extensive reading, enables the reader to compare the three processes very easily. No discussion is necessary in this article.

Previous to the college year 1936–37 considerable work had been done in setting up instructions for procedure, in establishing forms for recording data and in doing practice welding. Such welding was necessary because there was no one available who could weld copper by any process. This article will mention only briefly the results from the amateur work. In the welding of four plates of $^1/_8$ inch thickness, four plates of $^1/_4$ inch and five of $^5/_{16}$ inch, average efficiencies of 72.6, 75 and 50%, respectively, were obtained, all based upon the ultimate strength of the original plate at 30,900 psi. Some sections of the plates tested as high as 95% efficient. The beads on the $^5/_{16}$-inch plates had been reduced to the thickness of the plate itself, which explains in part the low efficiency for those five welds.

For the information of readers interested in these particular items, which are important to the welding of copper, there are appended to this article extracts from the literature on: desirable characteristics of filler rod for welding copper (1); a table of the tip size and head to be used with each thickness of plate for oxyacetylene welding of steel (9); a table of the size of carbon electrode and welding rod to be used for each plate thickness in the long carbon-arc process of welding copper (11). These extracts have been used with the permission of the authors and publishers.

During the college year 1936–37, through cooperation extended by the American Brass Company, we had the assistance of one of their expert welders. During one week in April 1937 this welder and the writer prepared and completed 29 welds on plates of thicknesses increasing from $^1/_8$ inch to $^1/_2$ inch, using filler rod which had been supplied by the above company. Twenty-six of these welds form the basis of this report.

Before welding, all plates used were stenciled according to the description given in Part 2 of the Appendix and as illustrated in Fig. 3 in this article for complete identification, and each plate of the pair was tested for hardness at from twelve to fifteen points on each section according to the positions shown in Fig. 5. After welding, the plates were sawed into coupons and again tested for hardness in the same areas as first indicated for hardness. The coupons were then reduced to the dimensions given in Fig. 4 for a destructive test in tension. From the data recorded and the autographic curves of the tests the results here reported were secured. Before any coupons from any weld were placed in test one was selected from each weld to be used for microscopic examination and chemical analysis. Photomicrographs were taken of each such sample. The Monotron Hardness Indicator was used for taking the hardness number of the plates because of the short time required for indicating each area. The coupons selected for tensile tests were tested by means of an Amsler Universal Testing Machine with autographic attachment. An automatic record of each test was made and filed.

Nine plates were welded by the oxyacetylene process following procedure set up from the review of the literature and varied according to the experience of the assisting expert welder. The data recorded at the time of welding, and other relevant information, have been condensed into the results shown in Table 4. All plates were clamped to a cast-iron working table during welding. All welds were made forehand as illustrated in Fig. 1 for oxyacetylene welding. From the nine plates welded eighty-four coupons were sawed and prepared for testing and sixty-three coupons tested in tension. Of the sixty-three tested one failed at the low efficiency of 26.3% for the minimum and one failed with an efficiency of 89.3% as the maximum. These efficiencies are based on the ultimate strength of soft copper at 30,000 psi. From a study of the table it will be noted that the plates welded with phosphor bronze filler rod containing 1.75% tin have a higher average efficiency than those welded with rod having 5% tin content. In each case the tip size used in welding these plates was three sizes larger than recommended for the nearest thickness of steel plate, which corroborates the recommendation given in Table 1 (5, 7).

The per cent of elongation in eight inches was less than the per cent of elongation between the two inches of the gage length next to the weld, because the metal adjacent to the weld was annealed more from the welding heat than those portions at the extremes of the gage length. This is also borne out by the dimensions of the tested coupon, which had a greater reduction in area next the weld than at the outer edge of the plate. Examination of the fractures showed a great deal of porosity in spite of the use of the flux[1] which was painted onto the plate and rod. There was some porosity in the adjacent plate also. The average speed of welding for eight of the plates was 3.07 inches per minute.

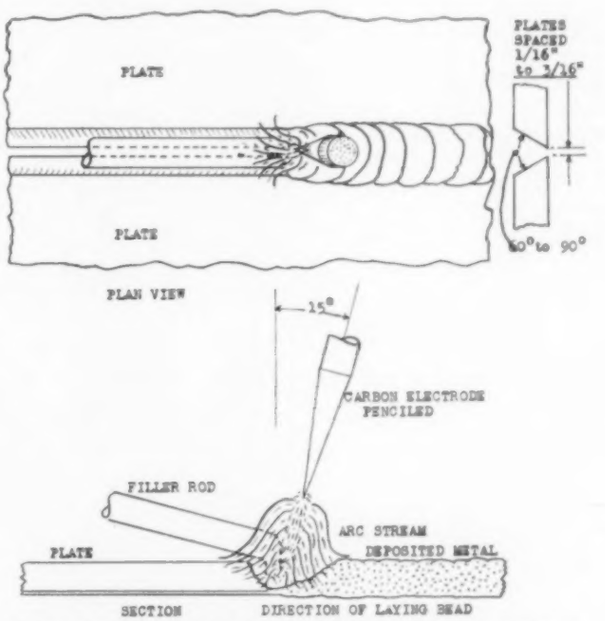


Fig. 2—Detail Showing Relative Position of Electrode, Filler Rod and Plates for Long Carbon-Arc Welding

[1] One part borax and three parts boric acid dissolved in hot water, and additions of a commercial brazing flux when required.

Table 2—Pressure Table for Oxweld Welding Blowpipe

Thickness of Metal		Types W-1, W-10, W-11, W-14			Types W-15, W-17, W-22		
			Oxygen Pressure Lb. per Sq. In.			Oxygen Pressure Lb. per Sq. In.	
Inch	Gage	Size of Welding Head	W-11, W-14	W-1, W-10	Size of Welding Head	W-15	W-17, W-22
	32	0	9				
	28	1	9		1	16–24	
	25				2	16–24	
1/32	22	2	10	10	3	16–24	
1/16	16	3	10	10	4	16–24	
3/32	13	4	11	11	5	16–24	
1/8	11	5	12	12	6	16–24	10–12
3/16		6	14	14	7	16–24	13–15
1/4		7	16	16	8		13–15
5/16		8		19	9		14–16
1/2		10		21	10		16–18
5/8					11		17–19
3/4		12		25	12		19–21
1		15		30	13		27–29
and		20		35			
over		30		45			

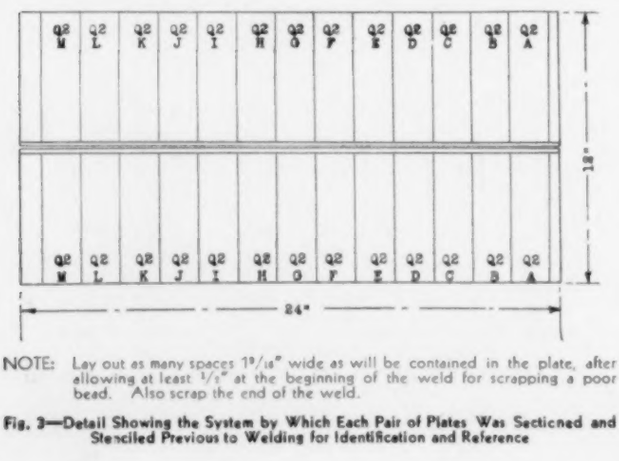
NOTE: Head sizes given are for steel, wrought iron and aluminum. Special welding conditions may require the use of a head size larger or smaller than given.

NOTE BY AUTHOR: For copper, welding tip should be two sizes larger than tip used for same gage steel plate (5, 7).

Twenty plates of varying thickness were welded by the long carbon-arc process, using as filler phosphor bronze welding rod containing 10%, 9.65% and 5% tin. The bead was laid forehand as illustrated in Fig. 2. These plates were cut into 184 coupons and reduced to the dimensions shown in Fig. 4. Of the total, 114 were tested in tension. The results are condensed into Table 5.

The outstanding facts from the table are these. The maximum strength of any one coupon was 30,600 psi and the minimum 7200 psi. The plate having the greatest efficiency, based on soft copper at 30,000 psi, tested 104%. Using the tensile strength of the original plate as a base, another plate had an average efficiency of 70.7%, as maximum of the twenty. Tensile tests indicate that the phosphor bronze rods with 10% and 9.65% tin content produce about the same quality weld. That rod which contains 5% tin does not test as strong, even though welding procedure was the same in all cases.

The bead on one coupon from several welds was reduced to the thickness of the plate before testing in tension. The point of failure was not shifted in any coupon because of this reduction. The amperes used in welding at a 50-volt setting of the 600-ampere generator were from 150 to 200 higher than the amount of current indicated in Table 3 of Part 1 of the Appendix. No automatic records were made but ampere readings were carefully observed. The backing bar used on all welds was of copper with a small groove machined in its center line for forming the back bead, and the plates were rigidly clamped to this bar. The coupons which tested low in tensile strength were those cut from that portion of the weld where penetration was not complete to the bottom of the "V."



NOTE: Lay out as many spaces 19/16" wide as will be contained in the plate, after allowing at least 1/2" at the beginning of the weld for scrapping a poor bead. Also scrap the end of the weld.

Fig. 3—Detail Showing the System by Which Each Pair of Plates Was Sectioned and Stenciled Previous to Welding for Identification and Reference

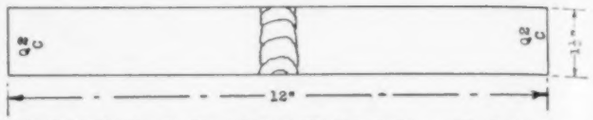


Fig. 4—Detail Showing the Dimensions to Which Each Coupon Was Reduced After Sawing from Plate

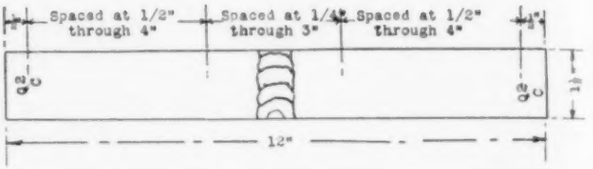


Fig. 5—Detail Showing Region Near Which Hardness Numbers of Surface Were Taken

The speed of welding for the 20 plates averaged twenty inches per minute. On the thicker plates, if the speed of welding was near this average, the fracture occurred between filler metal and plate. If the welding speed was somewhat less than the average, failure was in the plate itself at the edge of the top bead.

As previously mentioned, one coupon from each weld was selected for microscopic examination. From the twenty-nine welds twelve were picked as representative of the different welds with each kind of rod used for each thickness of plate. They are presented here with explanations of each print.

Figure 6 is a full-size photo of a 0.233-inch plate welded

Table 3—Table of Recommended Rod Sizes and Approximate Currents for "Long Arc" Welding Ordinary Copper

Thickness of Copper			Diameter of Rods		Approximate
U. S. Gage	Decimal Inches	Fraction Inches	Carbon Electrode	Filler Rod	Welding Current
18	0.05	1/20	3/16 in.	3/32 in.	80 amps.
17	0.0563	9/160	3/16 in.	3/32 in.	90 amps.
16	0.0625	1/16	3/16 in.	1/8 in.	90 amps.
15	0.07	9/128	3/16 in.	1/8 in.	100 amps.
14	0.078	5/64	1/4 in.	5/32 in.	130 amps.
13	0.094	3/32	1/4 in.	5/32 in.	135 amps.
12	0.109	7/64	1/4 in.	5/32 in.	140 amps.
11	0.125	1/8	1/4 in.	3/16 in.	150 amps.
10	0.141	9/64	1/4 in.	3/16 in.	160 amps.
9	0.156	5/32	1/4 in.	3/16 in.	165 amps.
8	0.172	11/64	1/4 in.	3/16 in.	170 amps.
7	0.1875	3/16	1/4 in.	3/16 in.	185 amps.
6	0.203	13/64	5/16 in.	1/4 in.	200 amps.
5	0.219	7/32	5/16 in.	1/4 in.	200 amps.
4	0.234	15/64	5/16 in.	1/4 in.	205 amps.
3	0.25	1/4	5/16 in.	1/4 in.	215 amps.
2	0.266	17/64	5/16 in.	1/4 in.	225 amps.
1	0.281	9/32	5/16 in.	5/16 in.	250 amps.
1/0	0.3125	5/16	5/16 in.	5/16 in.	250 amps.
2/0	0.344	11/32	5/16 in.	5/16 in.	255 amps.
3/0	0.375	3/8	5/16 in.	5/16 in.	270 amps.
4/0	0.406	13/32	5/16 in.	5/16 in.	290 amps.
5/0	0.4375	7/16	3/8 in.	3/8 in.	300 amps.
6/0	0.4688	15/32	3/8 in.	3/8 in.	310 amps.
7/0	0.5	1/2	3/8 in.	3/8 in.	325 amps.

NOTE: When the proper length of arc is maintained, welding voltage on copper under 1/16 in. should be 35 to 40 volts—and 40 to 50 volts on copper 1/16 in. and heavier.

TABLE 4

RESULTS OF PHYSICAL TESTS ON COPPER PLATE WELDED BY THE OXYACETYLENE PROCESS

Plate Number	Plate Thickness	Length of Weld in Inches	Coupons Tested	Tensile Strength Maximum, PSI	Tensile Strength Minimum, PSI	Tensile Strength Average, PSI	Tensile Strength of Plate, PSI	Efficiency Based on Plate Strength	Efficiency Based on 30,000 PSI	Elongation in 2" at Weld	Elongation in 8" Gauge Length	Tin Content of Phosphor Bronze Filler Rod Used	Size of Tip Used for Welding	Size of Tip Used in Preheating	Oxygen Pressure	Acetylene Pressure	Welding Speed in Inches per Minute	Fractures in Weld	Fractures in Plate
V3 A to K	0.115	17	4	19,300	17,300	18,200	50,700	35.9%	60.6%	4.45%	2.87%	SILICON ROD	No. 8	None	20	10	.968	4	0
W7 A to J	0.159	15.5	4	23,800	16,400	18,500	50,400	36.8%	61.7%	9.28%	5.93%	5%	No. 8	No.12	25	8	3.72	3	1
W8 A to J	0.158	15.5	4	21,300	18,500	19,400	50,200	38.7%	64.7%	8.89%	5.17%	5%	No. 8	No.12	25	8	2.82	2	2
W9 A to K	0.156	17	4	19,800	18,000	18,600	49,300	37.7%	62%	10.6%	5.58%	5%	No. 8	No.12	25	8	4.25	4	0
W10 A to K	0.160	17	4	26,800	23,300	24,700	49,100	50.2%	82.4%	16.6%	11.2%	5%	No.10	No.12	25	6	2.83	2	2
X4 A to H	0.233	12	3	19,900	14,600	16,900	42,500	39.8%	56.4%	12%	7.67%	1.75%	No.10	No.12	28	7.5	2.94	3	0
X4 I to Q	0.233	11	3	20,800	18,100	19,100	42,500	45%	63.6%	13%	8.67%	1.75%	No.10	No.12	27	8	2.36	3	0
X5 A to H	0.233	12	3	20,400	18,600	19,600	42,500	46.1%	65.4%	10.3%	7.83%	1.75%	No. 8	NO.12	25	6	1.8	3	0
X5 I to Q	0.233	13.5	4	23,200	15,900	20,500	42,500	48.2%	68.4%	14.2%	9.12%	1.75%	NO.10	No.12	28	6	3.1	4	0

with $^5/_{16}$-inch phosphor bronze rod containing 9.65% tin (weld X1). This shows the porosity at the top of the bead, the heavy back bead and to a limited extent the grain of the filler metal, which is brought out by the etching. At the bottom of the "V" formed by the two plates is a fusion zone which is shown in Fig. 7. The light areas are copper, the darker the filler metal. This photomicrograph shows that the copper plate was fused by the heat of welding but that the puddle cooled before the copper mixed with the filler metal. At this point in the weld there was complete penetration. The zone shown in Fig. 7 is at the very bottom edge of the bevel of the plate.

Figure 8 is a full-size photo of two welds on plates 0.233 inch (X2) and 0.254 inch (Z2 on right). The left weld was made with phosphor bronze filler rod containing 10% tin and the right weld with rod having 9.65% tin. The left weld shows lack of full penetration, which was due to a fast welding speed of 26.8 inches per minute. It also had high porosity near the top of the bead. The actual surface was smooth and had no pits. The right-hand weld shows complete penetration, which was made possible by a better spacing of the plates and by a slower welding speed (22.3 inches per minute). There appears to be no porosity in the weld metal. The left weld tested 79% and the right 88% efficient, based on soft copper.

Figure 9 is a photomicrograph at 85X showing the fusion line between filler and base metal on coupon X2-D. There is a finger of copper (light) protruding into the filler metal (dark). Three large grains of the filler metal contact nine grains of the base plate at the same boundary. The dark round spots indicate some porosity of both metals.

Figure 10 is a photomicrograph at 450X of 0.16-inch copper plate welded with phosphor bronze having 10% tin content. This shows the grains of the copper bordering one grain of the filler metal. The darker grains seem to extend into the grain of the weld metal, indicating that the deposited metal tends to inherit the grain of the parent plate. At the high magnification (450X), only a portion of one grain of the filler metal is visible in the photomicrograph. Its granular appearance is incident to the composition of the particular filler rod employed.

Figure 11 is a full-size photo of coupon K1-H selected from a weld made on 0.5-inch plate. This was the first plate welded using the double weld and double "V" and phosphor bronze containing 10% tin as filler rod. The plates were butted at the bottom of the "V" to prevent icicles from the first weld, and this caused incomplete penetration from both sides. The welding speed was high for this thickness of metal and there was no preheating; the result was a weld with 65% efficiency based on soft copper. The dark spot is a cold shut.

Figure 12 is a photomicrograph at 100X of the tip of the "V" on the top half of the weld (Coupon K1-H).

Fig. 6—Full-Size Photo of Coupon X1-L

Fig. 7—85 X. Coupon (X1-L)

Fig. 8—Full-Size Photo of X2-D and Z2-D

Fig. 9—Weld Coupon X2-D 85X

This magnification brings into focus the incomplete fusion and the lack of penetration. The black portion shows parts with no adhesion. The fact that one edge of the plate is irregular indicates softening of the plate but not complete fluidity; the fact that the other edge is straight points to low heat at this side of the weld. There is also some porosity.

Figure 13 is a photomicrograph of the fusion line taken at 200X on coupon V8-B to show two zones of freezing—the coarse crystals of the weld metal, and the border zone between the filler metal and plate where rapid cooling occurred. The light section, showing small grains, is the parent plate. The weld was made with a commercial brazing rod and was deposited by the oxyacetylene process.

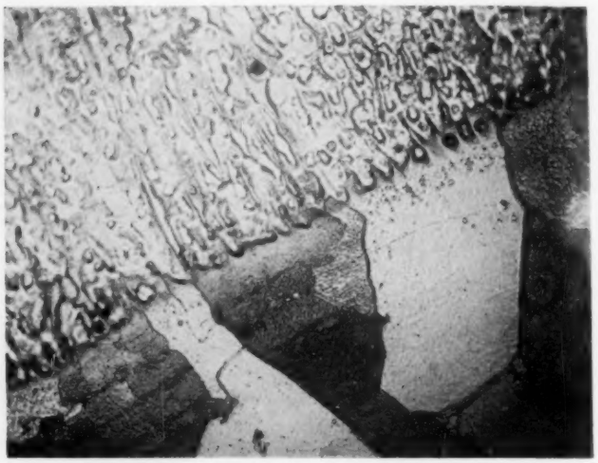


Fig. 10—Weld Coupon W4-H. 450X

TABLE 5

RESULTS OF PHYSICAL TESTS ON COPPER PLATE WELDED BY THE CARBON ARC PROCESS

Plate Number	Plate Thickness Inches	Length of Weld in Inches	Coupons Tested	Tensile Strength Maximum, PSI	Tensile Strength Minimum, PSI	Tensile Strength Average, PSI	Tensile Strength of Plate, PSI	Efficiency Based on Plate Strength	Efficiency Based on 30,000 PSI	Elongation in 2" At Weld	Elongation in 8" Gage Length	Tin Content of Phosphor Bronze Filler Rod Used	Voltmeter Reading	Ammeter Reading	Speed of Welding Inches per minute	Fractures in Weld	Fractures in Plate
U5 A to I	0.120	12.5	4	30,600	27,700	29,100	48,100	61.9%	97%	11.7%	2.43%	10%-	50	250-325	21	0	4
U5 J to R	0.120	14	5	30,800	27,000	28,600	48,100	59.5%	95.4%	11.7%	2.80%	10%-	50	300	22.5	0	5
U6 A to I	0.117	14	6	31,600	27,900	29,500	48,100	61.3%	98.3%	12.4%	3%	10%-	52	250-300	24	0	6
U6 J to R	0.117	14	5	30,900	28,300	30,000	48,100	62.4%	100%	14.3%	3%	10%-	53	340	17.5	0	5
V9 A to L	0.115	18.5	3	31,300	29,600	30,600	50,700	60.5%	102%	13.3%	3.2%	10%-	45	300	23	0	3
V10 A to K	0.115	17	4	30,700	28,800	29,800	50,700	58.7%	99.5%	14.3%	3.75%	10%-	49	350-400	27	0	4
W3 A to J	0.159	15.5	3	30,200	29,200	29,800	50,200	59.5%	99.3%	22.5%	6%	10%-	55	300-350	18	0	3
W3 K to S	0.160	14	3	31,300	31,300	31,300	50,200	62.5%	104%	25%	3.3%	10%-	50	300-325	16.5	0	3
W4 A to J	0.159	15.5	4	30,000	29,400	29,600	50,200	59%	98.7%	17.8%	4.66%	10%-	30	350-400	14.3	0	4
W4 K to S	0.159	14	6	29,100	27,400	29,800	50,200	59.2%	99.4%	15.1%	3.87%	10%-	55	250-275	12.9	1	5
X1 A to H	0.233	12	3	30,600	24,800	28,200	42,500	66.4%	94%	21.7%	6.83%	10%-	55	450	14.4	0	3
X1 I to P	0.233	12	2	31,500	31,000	31,300	42,500	73.5%	104%	25%	7.83%	9.65%	55	450-500	20	0	2
X2 A to H	0.233	12.5	2	30,500	23,800	27,200	42,500	63.9%	90.6%	16.2%	4.33%	5%	50	450-500	26.8	1	1
X2 I to Q	0.233	13	3	29,100	27,200	27,900	42,500	65.6%	93%	19.8%	5.25%	5%	55	?	22.3	1	2
X3 A to H	0.233	12.5	3	17,100	16,600	16,800	42,500	39.6%	56%	9.67%	2.17%	5%	45	375-425	24.2	3	0
Z1 A to L	0.254	18	3	28,000	24,800	26,000	37,600	69%	86.3%	14.7%	3.70%	9.65%	50	450-500	15	2	1
Z2 A to H	0.254	12.5	3	26,900	26,200	26,600	37,600	70.7%	88.7%	16.5%	4.57%	9.65%	55	350-425	22.8	0	3
X3 A to H	0.311	12	5	17,400	7,200	13,400	40,000	33.5%	44.7%	7.5%	1.70%	9.65%	55	600	14.4	5	0
K1 A to L	0.503	17.5	4	22,800	21,900	22,400	36,300	61.6%	74.5%	11.1%	2.90%	10%-	55	550	21	4	0

Figure 14 is a full-size photo of two welds (W10-B and W8-A) made on 0.16-inch plate with phosphor bronze welding rod having 5% tin, using the oxyacetylene process. The porosity of filler metal throughout both welds is clearly visible. Photomicrograph at 100X of coupon W10-B brings into focus the good fusion between the plate and the weld metal, indicated by the irregular boundary between the two metals. (See Fig. 15.) The mottled area is the filler metal; no attempt was made at complete elimination of the scratches in polishing, since at this magnification the structure was adequately developed.

Figure 16 is a photomicrograph at 100X of weld between two plates (X4) of 0.233-inch copper formed by

Fig. 11—Actual Size Coupon K1-H. Double Weld on One-Half Inch Plate

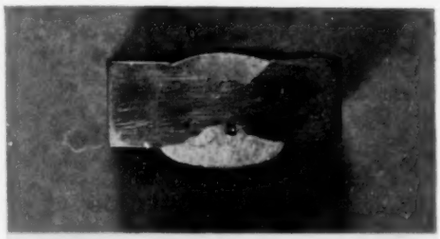


Fig. 12—Tip at Bottom of "V" on K1-H Photomicrograph Taken at 100X, Showing Lack of Adhesion and Penetration

the oxyacetylene process with phosphor bronze welding rod having 1.75% tin. The row of dots running diagonally across the photo defines the boundary, which otherwise would not be apparent. The black dots are the gas pockets in the filler metal. Larger grains show in the filler metal than in the plate. This weld had an efficiency of only 52% based on soft copper, because of the porosity and the length of time the plate was exposed to the welding heat.

Figure 17, taken at 100X, magnifies a field about $^1/_{16}$ inch to the side of the bottom edge of the bevel on coupon V9-C. The dark section of the photo is off the plate, the lighter portion is the base metal and the shaded part is the filler metal. Penetration on this plate was complete at the "V," and the heat of welding fused the plate back at

Fig. 13—Coupon V8-B. 200X

least $^1/_{16}$ inch from the bevel, almost melting through the bottom of the plate at the point shown in the illustration. This weld was almost perfect in every detail. It had an efficiency of 89% based on soft copper at 30,000 psi.

Figure 18 is a photo of bend tests made on two coupons from welds by carbon arc, W3-I (0.16 inch) and V10-B (0.115 inch). The failures, which are at the top edge of the bead, did not occur until additional pressure, placed on the loop, reduced the diameter assumed by the coupon when the 180° bend had been completed.

Before each pair of plates was welded a record was made of the surface hardness at 15 points on each section as scribed according to Figs. 3 and 5. After welding and sawing, the hardness of the coupons was determined, using the $^1/_{16}$ inch diameter ball on a Monotron Hardness Indicator. Plotting the hardness number as ordinate against the position on the coupon as abscissa gives the curves reproduced in Fig. 19. These show that there is a definite relationship between the point at which the strength of the plate is least, and the extent to which annealing temperatures have traveled laterally from the bead. There is also a relationship between the hardness number and the point of least strength. In every case the point where the curve drops suddenly is the point

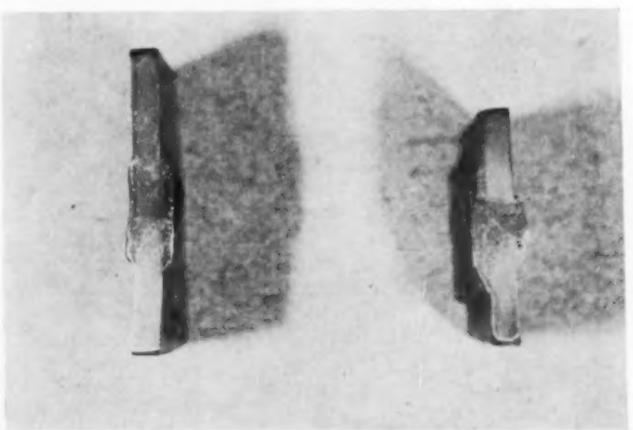


Fig. 14—W8-A at Right. W10-B at Left. Actual Size of Coupon Section

Fig. 15—Section of Fusion Line on Coupon W10-B at 100X

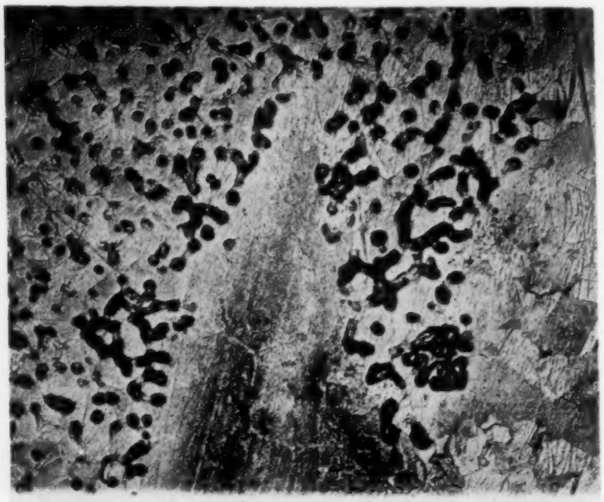


Fig. 16—Contact Line Between Weld Metal and Plate on Coupon X4-N, 100X

where the color of the plate has changed from metallic to a black or blue oxide—a change which has been due to a temperature near that for annealing copper. These color lines are coincident with the limits of greatest ductility of the metal in the coupon. These limits, which are clearly defined during the tensile tests, are shown distinctly in Figs. 20 and 22. Figures 21 and 23 are these same plates after welding but before sawing into coupons. The limit of the soft copper, the dark color line and the point where hardness number drops suddenly are all coincident. By observing the color change of bright copper, or by testing for surface hardness, a welder can know exactly to what extent the plate has been annealed by the welding heat. And, knowing the point where the plate is the softest, he can predict where failure of a joint would be most likely to occur.

Coupon W3-G was selected for microscopic examination. Photomicrographs at 300X were taken at definite points in the section of the plate in order to show the progressive annealing of the cold-rolled stock and the extent to which the welding heat causes recrystallization. Figure 25 is a composite of these photomicrographs. The hardness number of the surface and the distance from the center line of the weld are indicated for each position. Figure 25, Section A, taken at $1^{5}/_{16}$ inches from the center line of the weld, shows the typical directional structure of cold-rolled stock. Hardness number 31. Section B, at 1 inch from the center line of weld, shows partial recrystallization of the parent metal Hardness number 23. Section C, at $^{13}/_{16}$ inch from the center line of the weld, shows complete recrystallization. Hardness number 13. Section D, at $^{1}/_{4}$ inch from the center line of the weld, shows coalescence of the recrystallized grains. Hardness number 11. At the point corresponding to this position, failure occurred in all coupons tested in tension. It is evident from this study that the softening of the plate—that is, the complete recrystallization—and the boundary defined by the color lines on the surface are coincident. It is also evident that failure will occur at the edge of the bead, since the grains of recrystallized metal at the point nearest the zone of fusion will have coalesced to the greatest extent.

As a further check against the curves of Fig. 19, whose six curves represent averages of nine plates, an additional coupon was indicated for hardness. In this coupon hardness values were determined at twice as many points as before—namely, at points $^{1}/_{4}$ inch apart on the outside four inches and $^{1}/_{8}$ inch apart on the center four inches. The bead had been reduced to the thickness of the plate in order to make hardness indication easier. The plot of hardness number against position is shown in Fig. 24. The coupon was then tested in tension. The line of fracture, the color line and the line of demarcation between hard and soft copper as shown by reduction in area have all been marked on the curve sheet. These confirm the assertion made above, that there is a definite relationship between the point at which the strength of the plate is least, the extent to which annealing temperatures have traveled laterally from the bead, and the hardness number.

An examination of the failure lines of the carbon arc-welded plates showed practically every coupon failing at the edge of the bead in the plate. This leads to the conclusion that the softest part of the plate is the weakest and that the softest part will be next to the weld in the plate.

W10 was a plate welded with phosphor bronze having 5% tin content, using the oxyacetylene torch. This

Fig. 17—Showing Depth of Penetration on Weld of Plate V9. 100X

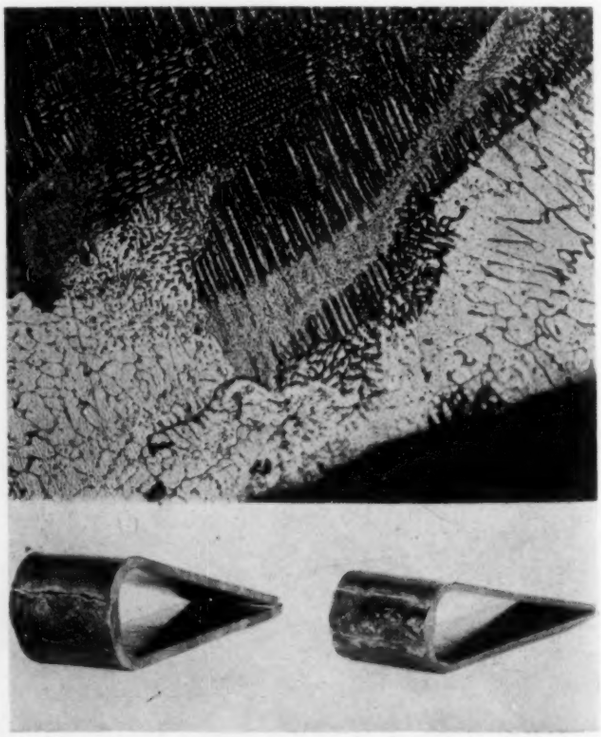


Fig. 18—Bend Tests of Coupons W3-1 (Left) and V10-B (Right). Failure Is at Top of the Bead

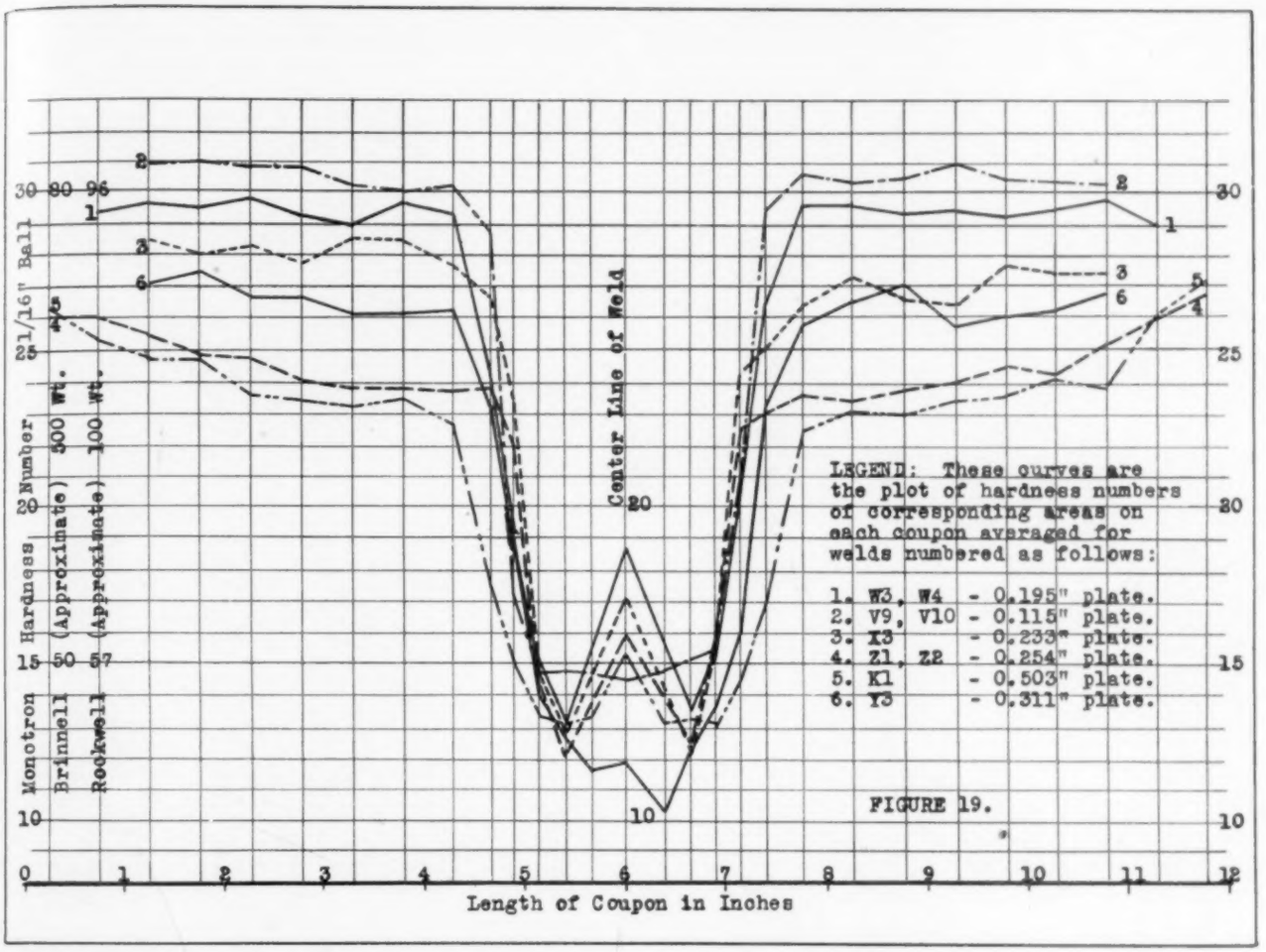

Monotron Hardness Number
Brinnell (Approximate) 500 Kg.
Rockwell (Approximate) 100 Wt.
1/16" Ball
30 80 96
25
20
15 50 57
10
Center Line of Weld
LEGEND: These curves are the plot of hardness numbers of corresponding areas on each coupon averaged for welds numbered as follows:
1. W3, W4 - 0.195" plate.
2. V9, V10 - 0.115" plate.
3. X3 - 0.233" plate.
4. Z1, Z2 - 0.254" plate.
5. K1 - 0.503" plate.
6. Y3 - 0.311" plate.
0 1 2 3 4 5 6 7 8 9 10 11 12
Length of Coupon in Inches

FIGURE 19.

plate is shown in Fig. 26 before it was sawed into coupons. Figure 27 is the same plate after the coupons had been tested in tension. The average efficiency of this weld was 70% based on soft copper. The line of failure was along the edge of the weld. Contrasting with this is Plate W7, which is shown in Figs. 28 and 29. The line of failure is almost in the center of the weld. This failure is due to a low bead throughout the weld. Both plates were welded with the same kind of rod, but the second plate had an efficiency of only 54% based on soft copper.

To complete a satisfactory weld on copper using oxy-acetylene these conditions are necessary: sufficient heat must be concentrated at the welding point to fuse both sides of the plate and the rod as rapidly as possible; the welding tip should be oscillated so as to weave from side to side of the groove; cold shuts must be prevented by maintaining a continuous puddle of plate and filler metal; the speed of welding should be consistent with the laying of a complete bead. Sufficient flux should be used on the rod and on the plate to provide for a protective coating over the puddle and adjacent plate at all times. Concentration of the flame at one point for too long a time

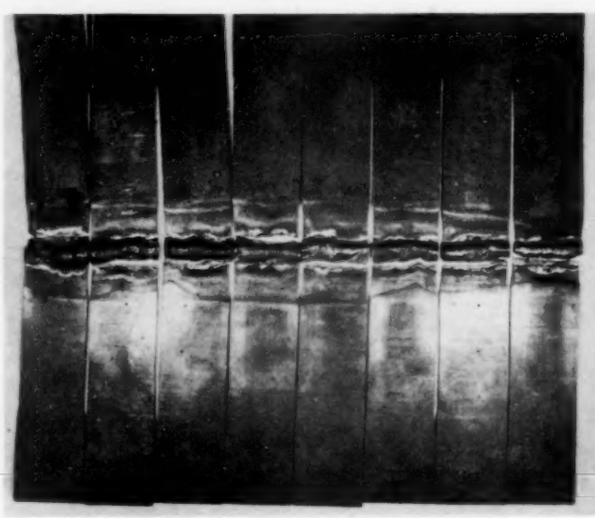


Fig. 20—Weld U5 After Test to Destruction in Tension

Fig. 21—Weld U5 After Welding But Before Sawing Into Coupons

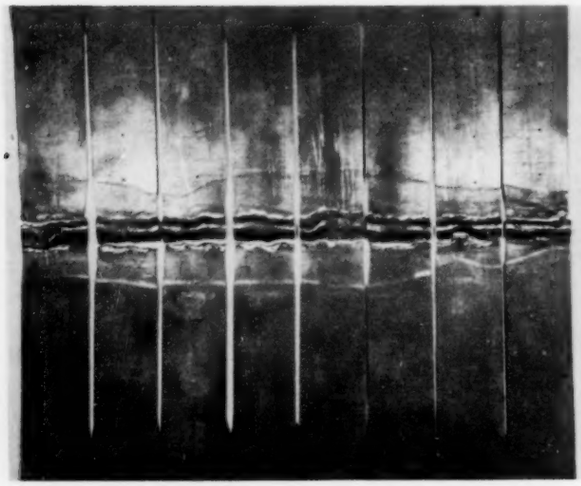


Fig. 22—Weld U6 After Tests in Tension

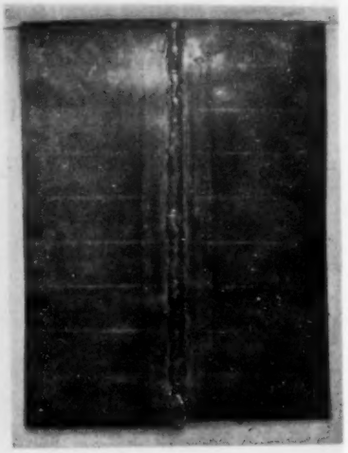


Fig. 23—Weld U6 Before Cutting Into Coupons

may cause burning of a hole, which must be filled. Lack of a fluxing agent will tend to reduce fluidity of puddle and to increase porosity.

When welding with the carbon-arc process, the operator must be constantly on the alert and observe: that the filler rod is held not more than $1/4$ inch above the groove and over its center; that the arc is struck from the rod and not from the plate at any point; that the arc impinges on the rod and not on the plate; that the puddle of fused metal is sufficiently fluid to flow avidly along the adjacent surface of the groove and at the same time to the bottom of the groove; that the arc is maintained at from one to one and one-quarter inch long; that speed along the weld is rapid enough to prevent burning of a hole in the plate, yet slow enough to permit fusing of the plate to the bottom of the groove for completing the back bead. If the speed along the weld is too slow, or the arc too short, the bead will be wide, black and undercut, or perhaps a hole will be burned in the plate. If the welding speed is too fast, the penetration will be only partial, the bead will be narrow and highly rounded, and the surface may be a bright red-oxide color. This color usually indicates good welding heat, but in view of the high speed of welding, good penetration may not have been made.

The expert welder, working by either process, learns these things, and his reaction to what he sees becomes automatic; but the apprentice must first be made conscious of what to look for, and must then learn to respond according to the instruction given.

The results of this research may be summarized in a

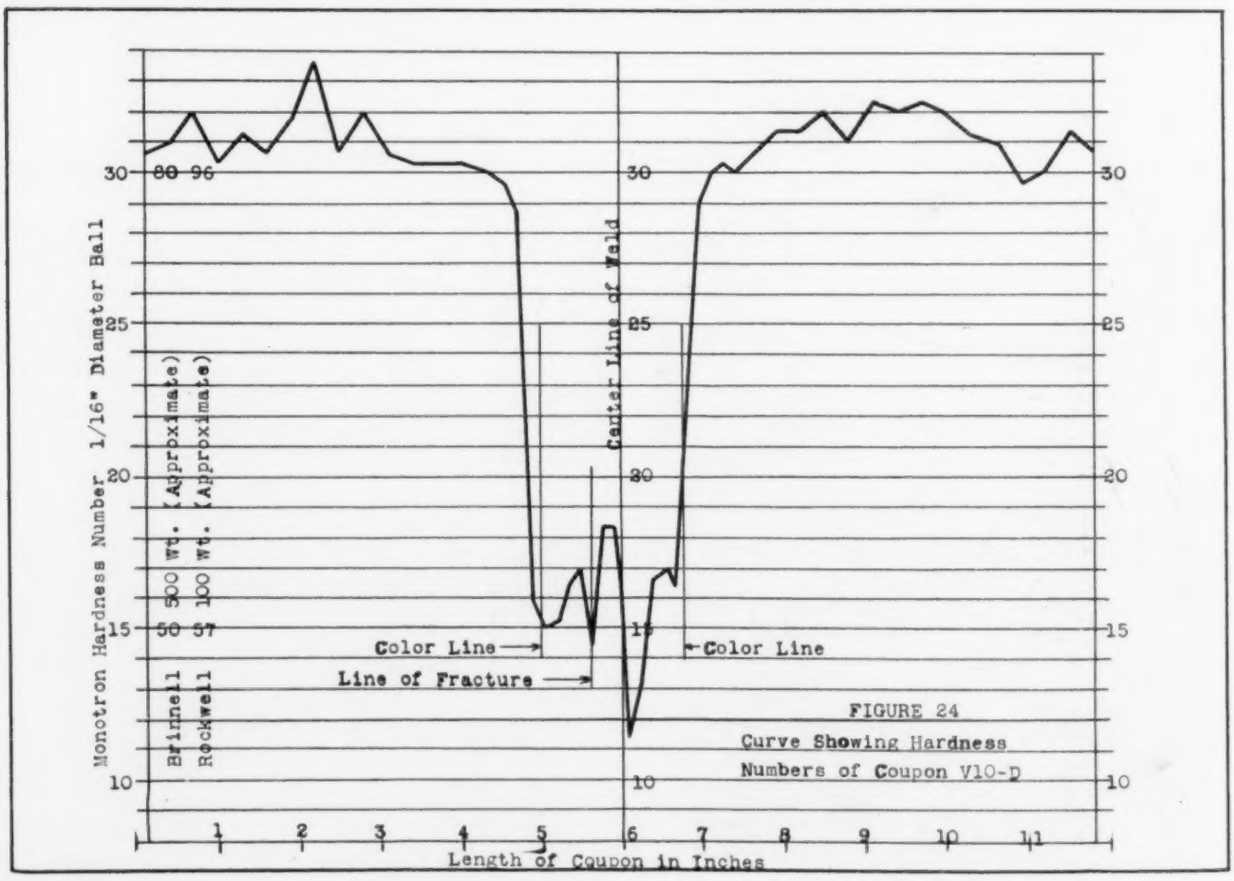
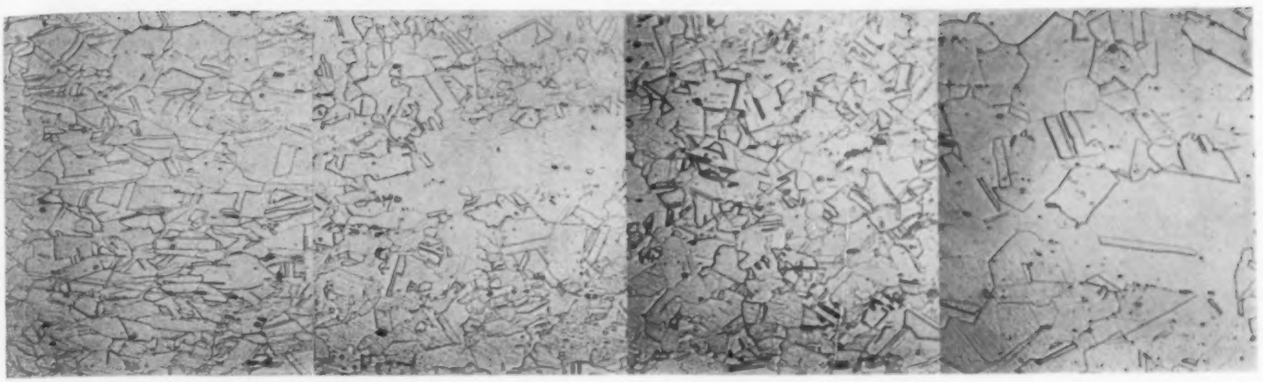


FIGURE 24
Curve Showing Hardness Numbers of Coupon V10-D



Section A
Showing Grains of Cold-Rolled Material. Hardness Number 31.

Section B
Showing Grains Partially Recrystallized. Hardness Number 23.

Section C
Showing Grains Completely Recrystallized. Hardness Number 13.

Section D
Showing Coalescence of Recrystallized Grains. Hardness Number 11.

Figure 25—Composite of Four Photomicrographs Taken at Definite Points on Coupon W3-G
Section A taken at $1\frac{5}{16}$ inches from center line of weld.
Section B taken at 1 inch from center line of weld.
Section C taken at $\frac{13}{16}$ inch from center line of weld.
Section D taken at $\frac{1}{4}$ inch from center line of weld.

few general statements. Under the oxyacetylene process: The efficiencies of plates are less than of those completed by the carbon-arc process, apparently because of the length of time the plate is exposed to welding temperatures. Preheating is necessary in gas welding for thicknesses greater than $\frac{1}{8}$ inch to provide for sufficient heat, to overcome losses by conduction, and to make possible faster welding rates. Plates should be so clamped that warping is prevented, especially when hard-rolled plate is welded. Edges of the plate on a butt joint must be so spaced that there is an increase in the spread between the edges of approximately $\frac{3}{16}$ inch per foot of length of plate, to prevent lapping of the plates at the end of the weld due to contraction during welding. This confirms the statement in the Review of the Literature by I. T. Hook given in Part 2 under oxyacetylene welding of copper. Unless a special rod requiring no flux is used, the weld metal should not be deposited without the use of flux; otherwise high porosity will result. This conclusion was confirmed by the microscopic examination; for it showed that where sufficient flux was used, less porosity occurred.

Fig. 26—Weld W10 After Welding

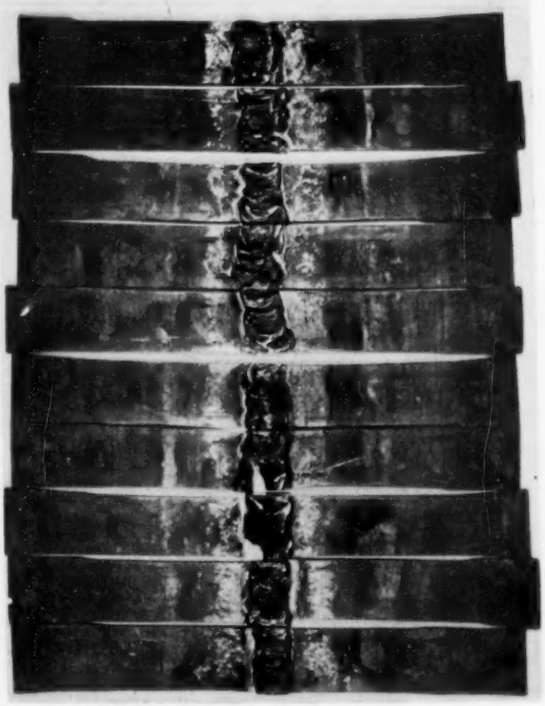


Fig. 27—Plate W 10 After Tension Tests

These general statements concerning the carbon arc process may be made: The speed of welding is about six times that of gas welding. The finished weld has higher efficiency than gas-welded plates. Preheating is not necessary if the speed is so regulated that full penetration is accomplished. Successful welds are completed when the speed of welding is just right; too slow a speed produces wide bead, undercutting and piled-up bead; too fast a speed reduces penetration, decreases strength of weld. Microscopic examination of long carbon arc welds revealed these facts clearly. Power consumed varies with thickness of plate, length of arc, nature of backing plate and voltage maintained. So many variables enter into the art of welding that no fixed relationship can be established.

The welding heat reduces the strength of any copper plate to that of soft copper; therefore 100% efficient joints are not the rule. The distance laterally to which this heat is dissipated varies inversely with the speed of welding and the thickness of the plate, and directly with the intensity of the arc stream. For thin plates the time of welding is shorter, and for thicker plates there is more material to absorb the heat although it is exposed to the welding heat for a longer time; hence, the linear distance to which this annealing effect is conducted seems to be the same for each thickness of plate. The location of fracture indicates that the weak point is at the softest section, as shown by the curves plotted from the hardness numbers and confirmed by microscopic examination.

This research project has not completely covered the possible field. There is still some work to be done on

copper plates of $^3/_8$ inch and thicker. It is planned to continue this research, welding by both processes, and to use other filler rods for comparison purposes. The results so far have been gratifying.

The assistance of the following persons is gratefully acknowledged: J. F. Ojala and S. C. Penberthy, graduates of M.C.M.T. working on research; M. Brandtberg of the Kenosha Branch of American Brass. The cooperation of the American Brass Company is greatly appreciated.

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9. Oxweld Instruction Manual by the Linde Air Products Company.
10. Fabrication of Oxweld Piping by the Linde Air Products Company.
11. "Long Carbon Arc Welding of Copper" published by Hobart Brothers.

Other articles which corroborate the information given in the summary of procedure are: The Welding of Copper-Nickel Alloys by Hans A. Hern, *Autog. Metallb.*, **24**, 110–112 (1931); Wilhelm Geldbach, On the Effect of Small Admixtures in the Welding Wire on the Welding of Copper, *Autog. Metallb.* **24**, 319–331, 335–345 (1931); and other articles in the *Journal of the Institute of Metals*, **38**, No. 2, 1927; *Metal Industry* **24**, 1926.

APPENDIX

Part One

(*A*) Desirable Characteristics of Filler Rod for the Welding of Copper

- (*a*) It should melt at a temperature slightly below that of copper.
- (*b*) It should be able to withstand a high superheat without loss or damage.
- (*c*) It should take to copper avidly when the copper is at a temperature from bright red heat to fusion or above.
- (*d*) It should have a higher strength as a weld metal than dead soft copper. The latter develops about 32,000 psi, so the weld metal should develop more than this to insure against failure in the weld metal itself.
- (*e*) It should be sound as a weld metal.
- (*f*) It should have a color and corrosion resistance not unlike copper.
- (*g*) The commercial welding rod that most nearly approaches these requirements is high in copper with sufficient tin to improve the strength and flowing properties, and sufficient phosphorus to make it flow at a temperature slightly below the melting point of copper and at the same time to protect it from oxidation at a high degree of superheat. As a cast metal (weld metal) it is generally sound and strong. Moreover, once melted, it takes to copper avidly when the latter is at a temperature above a bright red, alloying therewith to develop nearly its full strength.

Part Two

System Used for Identification of Plates and Coupons

The numbering system (see Figs. 3, 4 and 5) used for identifying records and coupons cut from welded plates proved to be very useful. It is as follows: Beginning with the letter "P" the different lots of copper plate received were lettered consecutively. Under this letter the plates cut in 20-inch lengths were numbered serially (1, 2, 3, etc.) in pairs until all pairs cut from that thickness had been stenciled as pairs. Under this identification number each coupon cut from the welded plate was stenciled with a letter beginning with "A" from the start of the weld to the letter corresponding to the finish of the weld for the final coupon in the welded plate. In this way the combination of letters before and after the number was not the same.

Fig. 28—Plate W7 After Welding

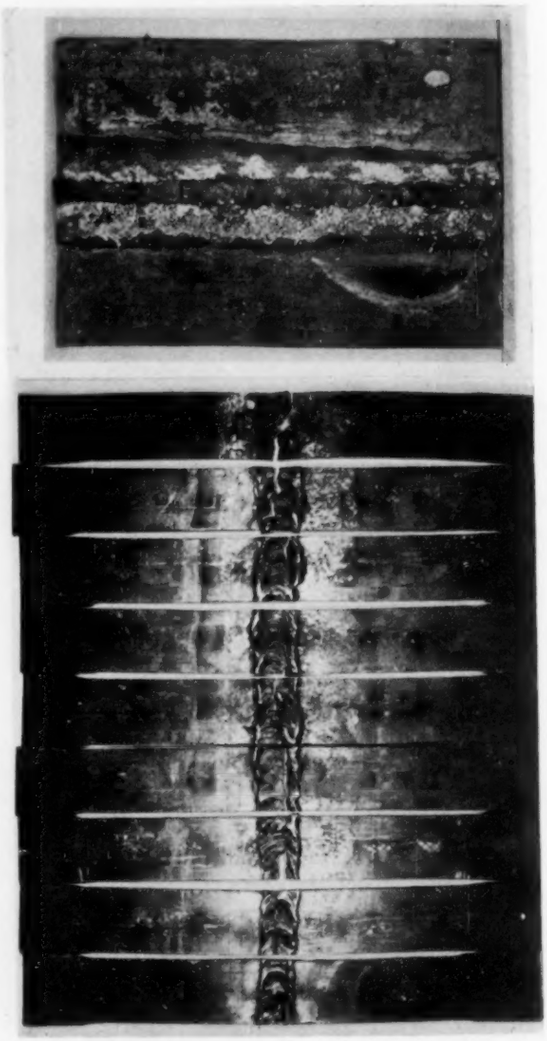

Fig. 29—Plate W7 After Tension Tests

Thus, Q2-C was the third coupon cut from welded plate No. 2 of lot Q and was identified for all data and computation, inspection and further reference. The form of the coupon as cut from the plate is shown in Fig. 4 in the body of the article. It was decided not to undercut the portion of coupon included within 8-inch gage marks as is usual for a flat test bar because: (1) to leave it straight would save considerable time and labor of machining, and (2) on coupons of uniform width and thickness the effect of the welding heat would show throughout the whole coupon and not at the section with the reduced cross-sectional area. Further, since the annealed copper has a lower tensile strength than hard copper no fractures would occur in the grips of the testing machine, the softest copper being nearest the weld metal.

In this article the plates welded and reported were numbered from "U" to "Z," with one plate lettered "K." All plates welded from lots of copper lettered from "P" to "T" were used in practice and not welded by expert welders.

Static and Impact Tensile Properties of Some Welds at Ordinary and Low Temperatures*

By OTTO H. HENRY†

Summary

COMPARISON of the static and impact tensile properties of resistance butt, atomic hydrogen and bronze welds in 0.25% carbon steel, and gas and arc welds in 18–8 shows that static tensile properties do not vary with temperature (+20 to −80° C.) in the same way as impact tensile properties. On the whole, the impact and static tensile properties are not greatly affected by temperature in the range under investigation. The static tensile properties of two types of oxyacetylene welds in structural steel at +20 to −80° C. are not adversely affected unless the weld is of low quality.

Introduction.—About one year ago at the Cleveland meeting of this society the author had the privilege of presenting a paper on the Tensile Impact Properties of Some Iron Steel and Welded Joints at Low Temperatures. Since that time additional tensile-impact tests have been made and static tension tests at the same temperatures and on the same materials have been made. It is the opinion of the author that a comparison of the results obtained by the two methods would be of particular interest mainly because of the fact that it has been shown by numerous investigators that there is no correlation between the notch-bar impact test and the static tension test and also because of the fact that the tensile impact test results show that no discontinuity in the properties of the welds occur at the temperatures when the notch bar tests show marked discontinuities. The results of these tests, showing as they do a fundamental difference between static and impact tensile properties at low temperatures, indicate the need for more research particularly in view of the low speed (10 miles per hour) of impact in the tests in comparison with the much higher speeds of railway and motor cars, and other even faster means of travel.

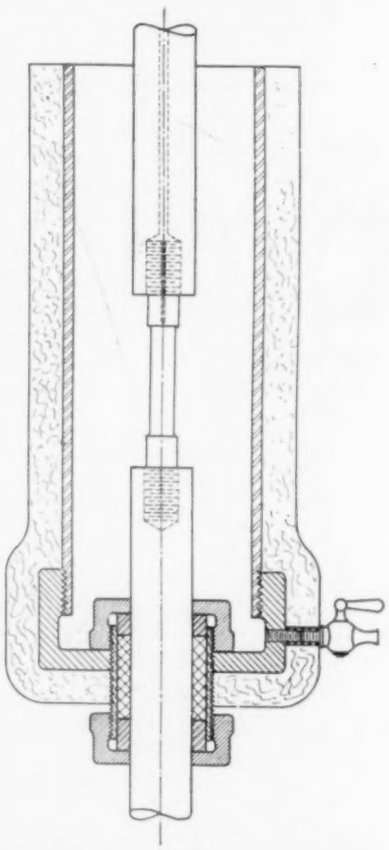

Fig. 1—Sectional Sketch of the Refrigeration Attachment—1/4 Size

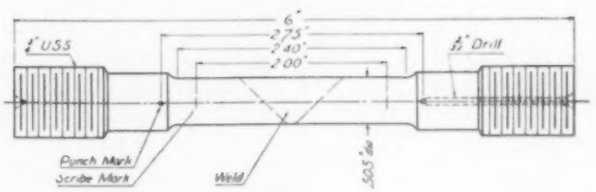

Fig. 2—Dimensions of Specimens

Methods of Test.—It would be tedious to describe all the work that has been done along the line of tensile testing at low temperatures with regard to methods, materials and results; therefore, only the method used in the present series of tests will be discussed.

It must be kept in mind that the tensile impact specimens were broken at a constant striking velocity of about 15 feet per second and that the so-called static tension specimens were broken at a velocity of about 0.001 feet per second. Also, all welded specimens were tested in the as-welded, unheat treated condition.

The tensile-impact specimen dimensions and a description of the test apparatus were given in the October 1936 issue of the AMERICAN WELDING SOCIETY JOURNAL.[1]

The static tensile tests were made on a 100,000 pound capacity Riehle Universal Testing Machine with a specially constructed refrigeration attachment shown in Fig. 1.

The refrigeration attachment is of very simple construction and is clearly shown in the sectional sketch. The main body of the flask consists of a section of 6-inch

* To be presented at Annual Meeting, AMERICAN WELDING SOCIETY, Atlantic City, Oct. 18–22, 1937. Contribution to Fundamental Research Division of Welding Research Committee.

Some of the data contained herein was obtained from thesis presented at The Polytechnic Institute of Brooklyn by Richard Shaw B.M.E. 1933, Peter L. Calamari B.M.E. 1934, Edgar A. Jahn B.M.E. 1935, Emil A. Imbembo M.M.E. 1935, Bernard Hirshkowitz B.M.E. 1936 and Gerard K. Wicks B.M.E. 1936.

† Assistant Professor of Mechanical Engineering at the Polytechnic Institute of Brooklyn.

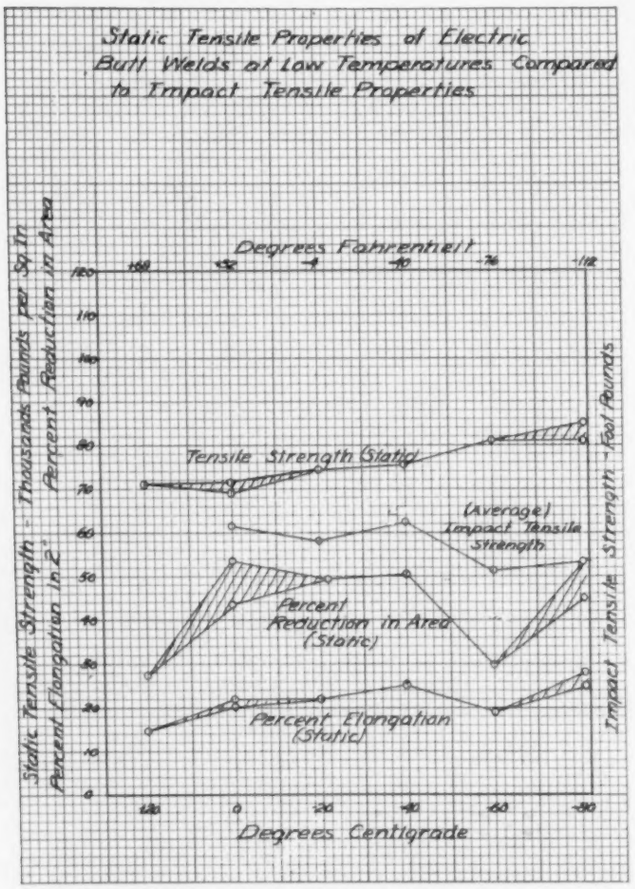
Static Tensile Properties of Electric Butt Welds at Low Temperatures Compared to Impact Tensile Properties
Degrees Fahrenheit
Degrees Centigrade
Static Tensile Strength - Thousands Pounds per Sq. In.
Percent Elongation in 2"
Percent Reduction in Area
Impact Tensile Strength - Foot Pounds
Tensile Strength (Static)
(Average) Impact Tensile Strength
Percent Reduction in Area (Static)
Percent Elongation (Static)

Fig. 3

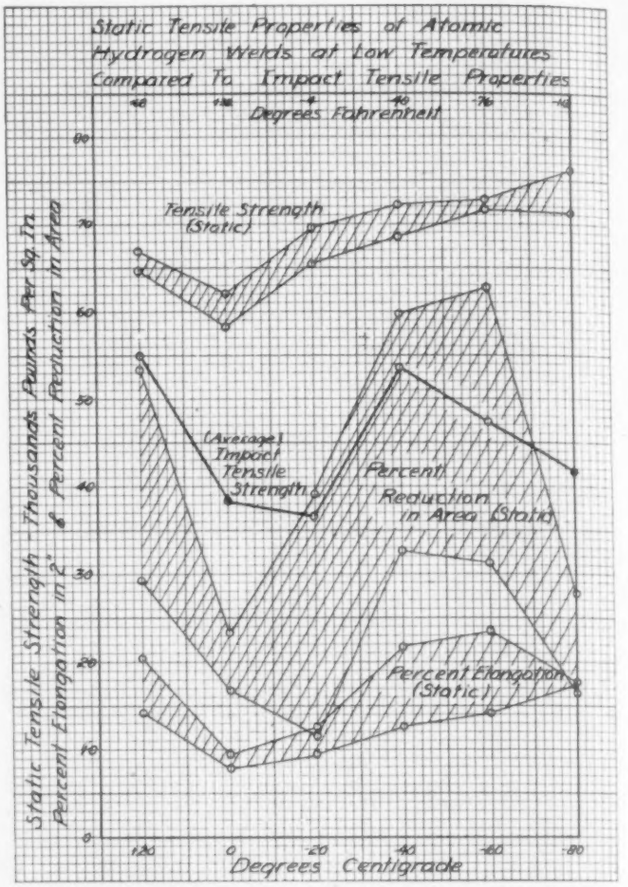
Static Tensile Properties of Atomic Hydrogen Welds at Low Temperatures Compared to Impact Tensile Properties
Degrees Fahrenheit
Degrees Centigrade
Static Tensile Strength - Thousands Pounds Per Sq. In.
Percent Elongation in 2"
Percent Reduction in Area
Tensile Strength (Static)
(Average) Impact Tensile Strength
Percent Reduction in Area (Static)
Percent Elongation (Static)

Fig. 4

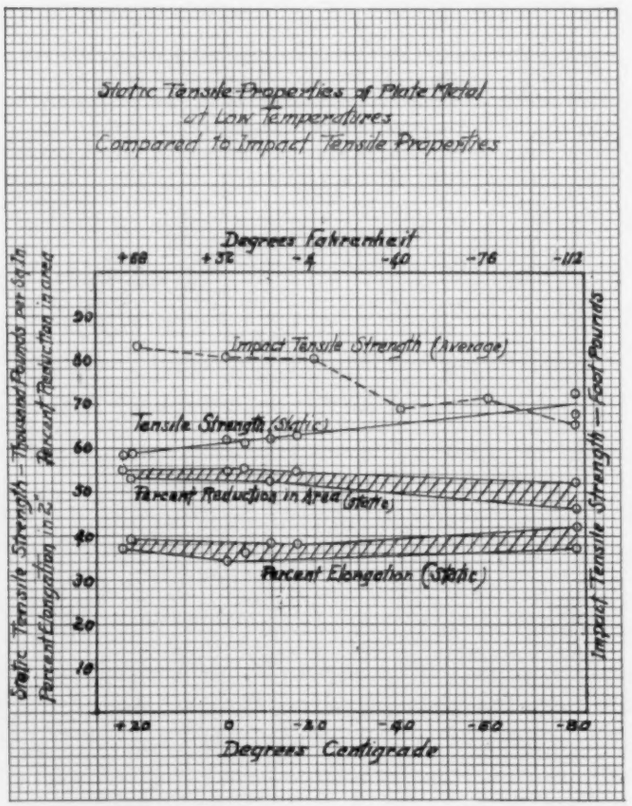
Static Tensile Properties of Plate Metal at Low Temperatures Compared to Impact Tensile Properties
Degrees Fahrenheit
Degrees Centigrade
Static Tensile Strength - Thousand Pounds per Sq. In.
Percent Elongation in 2"
Percent Reduction in area
Impact Tensile Strength - Foot Pounds
Impact Tensile Strength (Average)
Tensile Strength (Static)
Percent Reduction in Area (Static)
Percent Elongation (Static)

Fig. 5

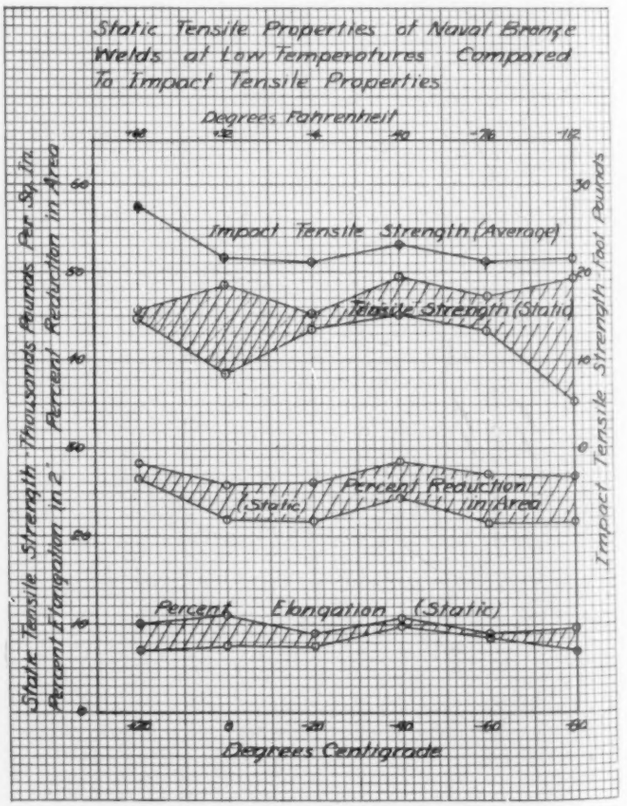
Static Tensile Properties of Naval Brange Welds at Low Temperatures Compared To Impact Tensile Properties
Degrees Fahrenheit
Degrees Centigrade
Static Tensile Strength Thousands Pounds Per Sq. In.
Percent Elongation in 2"
Percent Reduction in Area
Impact Tensile Strength - Foot Pounds
Impact Tensile Strength (Average)
Tensile Strength (Static)
Percent Reduction in Area (Static)
Percent Elongation (Static)

Fig. 6

pipe with a drilled and tapped cap at one end. Into this tapped cap a close reamed $1^1/_2$-inch pipe nipple is screwed and on each end of this nipple a pipe cap drilled with a $1^3/_8$-inch hole is placed in order to compress the metallic packing which surrounds the $1^3/_8$-inch diameter grip into which the specimen to be tested is screwed. The outside of the entire flask is covered with about one inch of hair-felt and tapped. The upper grip for the specimen is drilled the full length with a $^3/_{16}$-inch hole to permit the insertion of a thermocouple into the specimen. The protruding ends of both the $1^3/_8$-inch diameter grips are threaded and fitted with a plate and a nut which are supported during tests on the stationary and movable heads, respectively, of the testing machine.

Temperature Control.—For the purpose of temperature control the following cooling media were placed in the flask:

Medium	Approximate Temperature Attained
Water	20° C.
Ice and Water	0° C.
Alcohol and Solid CO_2	−20 to −80° C.

In order to keep the temperature constant at −20, −40, −60 and −80 it was necessary to drop in a few pieces of solid CO_2 every few minutes but after a little experience no difficulty was found in keeping the temperature as desired.

Temperature Measurements.—Two copper-constantan thermocouples were used for temperature measurements, one being placed in the hole provided for that purpose through the grip and into the end of the specimen, the other being kept in the liquid bath. A Leeds and Northrup portable potentiometer was used to measure the potential across the thermocouple leads.

Preliminary Tests.—Before starting the principal tests, a series of preliminary tests were made on cold-rolled steel specimens in order to determine the increase of temperature at various rates of pulling. For this purpose, the testing machine was run in its lowest gear, the motor speed being regulated by means of a rheostat in the armature circuit of the direct current driving motor. The only readings taken were the temperature of the specimen and the speed of the head of the testing machine. A small notch was made in each preliminary test specimen to cause it to break at a section as near as possible to the thermocouple. Ice and water were used to provide a bath of constant temperature. The results of these tests indicated that at a speed of 0.015 inch per minute, the increase in temperature was too small to measure.

Principal Tests.—In testing the welded specimens, the position of the rheostat handle was taken as a rough indication of the speed, but the average rate of pulling was determined for each test by dividing the total distance moved by the head of the machine by the total time required for the test.

No test was started until the readings obtained from the two thermocouples were constant and as nearly as possible in agreement. The temperature of the bath and of the specimen were noted at frequent intervals during the test and immediately adjusted if found to be varying.

Owing to the low speed of pulling and to the fact that many of the specimens were non-homogeneous, it was very difficult and often impossible to determine the yield

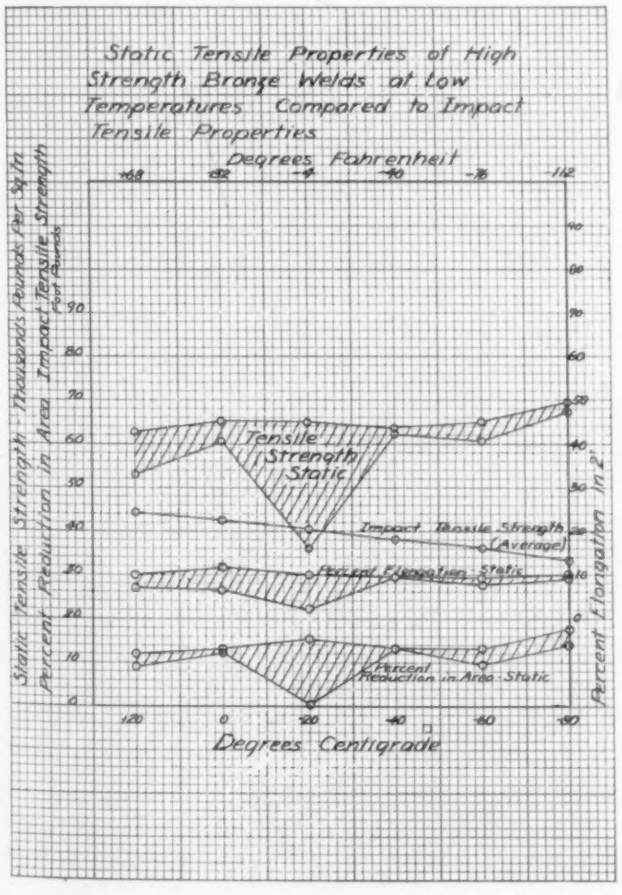
Static Tensile Properties of High Strength Bronze Welds at Low Temperatures Compared to Impact Tensile Properties
Degrees Fahrenheit
Degrees Centigrade
Static Tensile Strength Thousands Pounds Per Sq.In.
Percent Reduction in Area Impact Tensile Strength Foot Pounds
Percent Elongation in 2"
Tensile Strength Static
Impact Tensile Strength (Average)
Percent Elongation Static
Percent Reduction in Area Static

Fig. 7

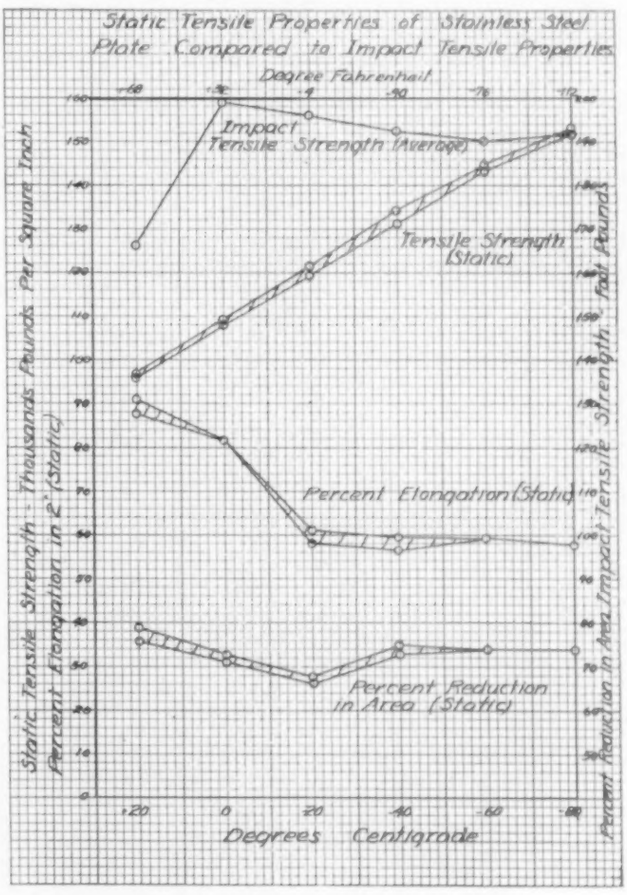
Static Tensile Properties of Stainless Steel Plate Compared to Impact Tensile Properties
Degree Fahrenheit
Degrees Centigrade
Static Tensile Strength Thousands Pounds Per Square Inch
Percent Elongation in 2" (Static)
Percent Reduction in Area Impact Tensile Strength Foot Pounds
Impact Tensile Strength (Average)
Tensile Strength (Static)
Percent Elongation (Static)
Percent Reduction in Area (Static)

Fig. 8

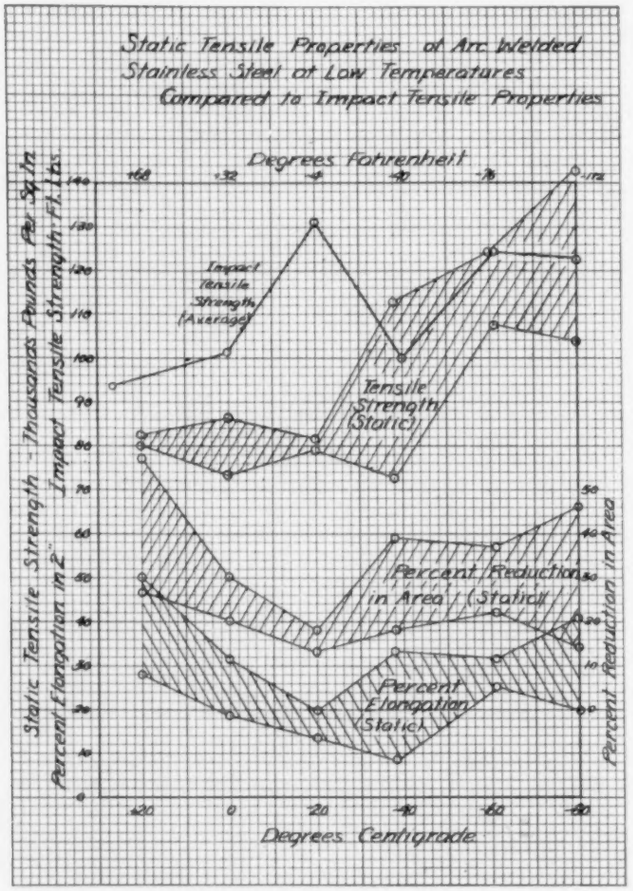
Static Tensile Properties of Arc Welded Stainless Steel at Low Temperatures Compared to Impact Tensile Properties
Degrees Fahrenheit
Impact Tensile Strength (Average)
Tensile Strength (Static)
Percent Reduction in Area (Static)
Percent Elongation (Static)
Degrees Centigrade

Fig. 9

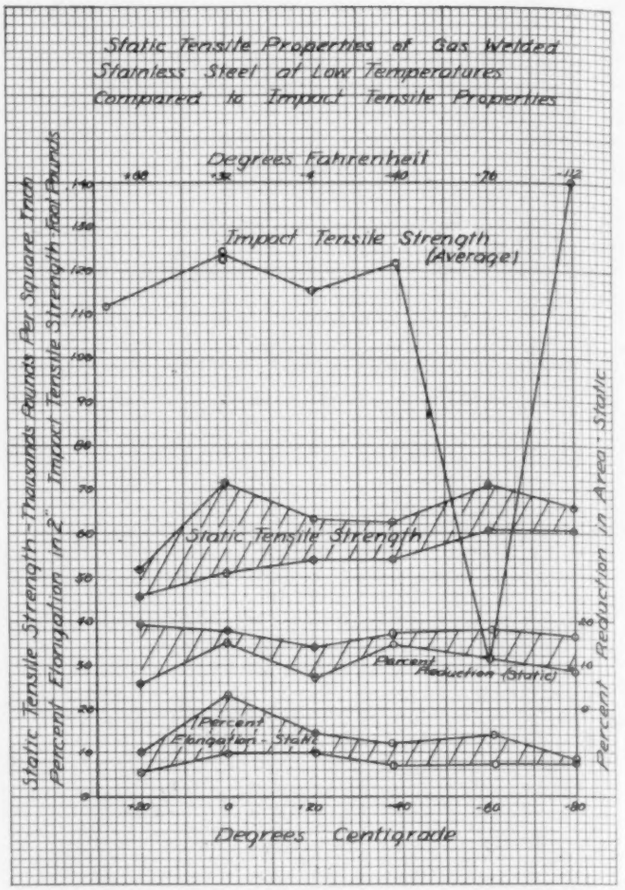
Static Tensile Properties of Gas Welded Stainless Steel at Low Temperatures Compared to Impact Tensile Properties
Degrees Fahrenheit
Impact Tensile Strength (Average)
Static Tensile Strength
Percent Reduction (Static)
Percent Elongation (Static)
Degrees Centigrade

Fig. 10

point from the "drop of the beam." A special extensometer was constructed and used on several of the specimens tested but the results obtained have been omitted from this paper.

The observed data from these tests are shown graphically with the spread indicated by the cross-hatched area. The results of the tensile impact tests which were reported last year have been plotted as average points on the same graph sheet in each case.

Materials Tested

(1) Twelve Naval Bronze welds, supplied by Air Reduction Sales Co.
(2) Twelve High-Strength Bronze Welds, supplied by the Union Carbide and Carbon Research Laboratories.
(3) Twelve Atomic Hydrogen Welds, supplied by the General Electric Company.
(4) Twelve Electric Resistance Welds, supplied by the Union Carbide and Carbon Research Laboratories.
(5) Twelve Stainless Steel Plate Metal.
(6) Twelve Stainless Steel Gas Welded.
(7) Twelve Stainless Steel Arc Welded, supplied by the Allegheny Steel Company.

The base material used for the Naval Bronze, High-Strength Bronze, Atomic Hydrogen and Resistance welds was of the following composition:

Carbon	0.25%
Manganese	0.57%
Sulphur	0.022%
Phosphorus	0.016%

The plates were $^3/_4$ inches thick and the procedure was to make the welds in plates that were beveled at 45 degrees single V so that the weld was at right angles to the direction of rolling. After welding the plates were cut into strips $^3/_4 \times {}^3/_4 \times 6$ inches with the welds centrally located. These strips were then centered and turned down in a lathe to the dimensions shown in Fig. 2.

The stainless steel plate metal had the following chemical analysis:

Carbon	0.06%
Chromium	19.89%
Nickel	9.21%

The material used for the welding rod in the stainless steel:

Carbon	0.083%
Chromium	19.72%
Nickel	8.91%

The composition of the high-strength bronze welding rod was:

Copper	58.00%
Zinc	39.85%
Tin	1.00%
Iron	1.00%
Silicon	0.10%
Manganese	0.05%

There is no definite information as to the composition of this metal after welding, according to the Union Carbide and Carbon Research Laboratories, but it is quite likely that there is some loss of silicon and zinc.

All the specimens were made three or four years ago.

Fig. 11—Micrograph of Gas-Welded 18-8 at Junction Zone. X100. Specimen Broken in Impact-Tension at +26° C. Large Grains Weld Metal

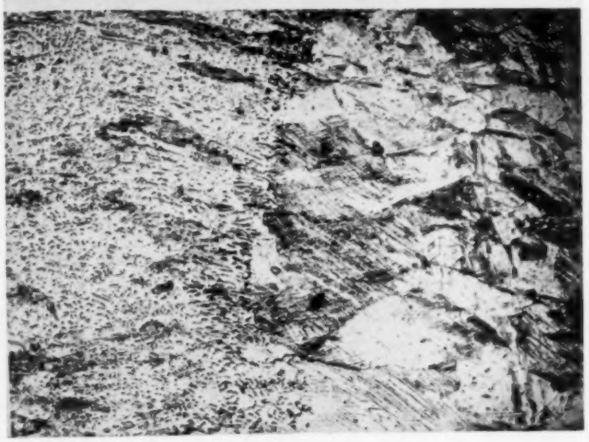

Fig. 12—Micrograph of Arc-Welded 18-8 at Junction Zone. X100. Note Dendrites in Weld Metal. Specimen Broken in Impact-Tension at +26° C.

Results of Tests

Since the tests were intended as a comparison of the static tensile and impact tensile values of welded and bronze-welded joints, the results are plotted graphically in Figs. 3 to 10. The impact tensile properties are taken mainly from an earlier paper.[1]

Fusion Welds in 0.25% Carbon Steel.—The results for resistance and atomic hydrogen welds are shown in Figs. 3 and 4. These may be compared with the static and impact tensile properties of unwelded base metal (0.25% C steel) shown in Fig. 5. The static strength and ductility of the resistance butt welds both increase with decrease in temperature. The higher strength and lower ductility of the welds as compared with the unwelded base metal is not unexpected in view of the comparatively wide heat-affected zone of the resistance welds. The impact tensile strength of the welds is 10 to 20 foot pounds less than the base metal and, like it, decreases with decrease in temperature. It should be noted that the elongation of the resistance butt welds in tensile impact decreases with decrease in temperature whereas the reduction of area does not. Evidently the deformation of the welded impact specimen is restricted to a smaller length of the specimen, the impact ductility that is represented by reduction in area remaining unchanged as the temperature is lowered. In static tension the behavior is otherwise, as Fig. 3 makes clear.

The atomic hydrogen welds (Fig. 4) gave unexpected results both in static and impact tension. In both types of test the strength and ductility decreased sharply at about 0° C. to increase again at −40 to −60° C. From the rather large spread in values it may be concluded that the atomic hydrogen welds were not of uniform quality. The maximum static values obtained at −40 to −60° C. (70,000 psi tensile, 60% reduction of area, 23% elongation in 2 inches) are comparable with unwelded base metal. The apparently low value of elongation in the welded as compared to the unwelded is due to the effect of the weld in restricting the length in which elongation occurs. Probably the reduction of area is a better basis than the elongation for comparing the ductility of a weld with unwelded base metal. In this connection the uniform elongation as contrasted with the local or the total elongation may be found to have significance in estimating the tensile ductility of a butt weld.

Bronze Welds.—Fracture of the bronze-welded specimens occurred in the weld, except in a few instances of faulty welds, which are not considered here. The static tensile properties of the Naval bronze welds, Fig. 6, showed very little change with temperature. On the other hand the high-strength bronze welds, Fig. 7,

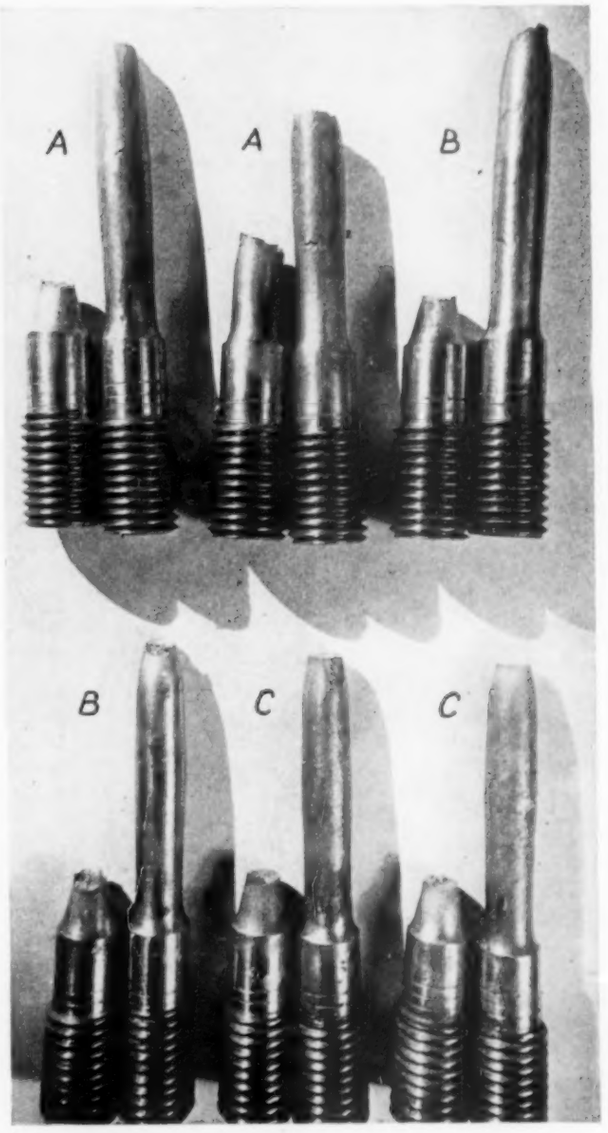

Fig. 13—Fractures in High-Strength Welds. A—At Room Temperature. B—At −15° C. C—At −80° C.

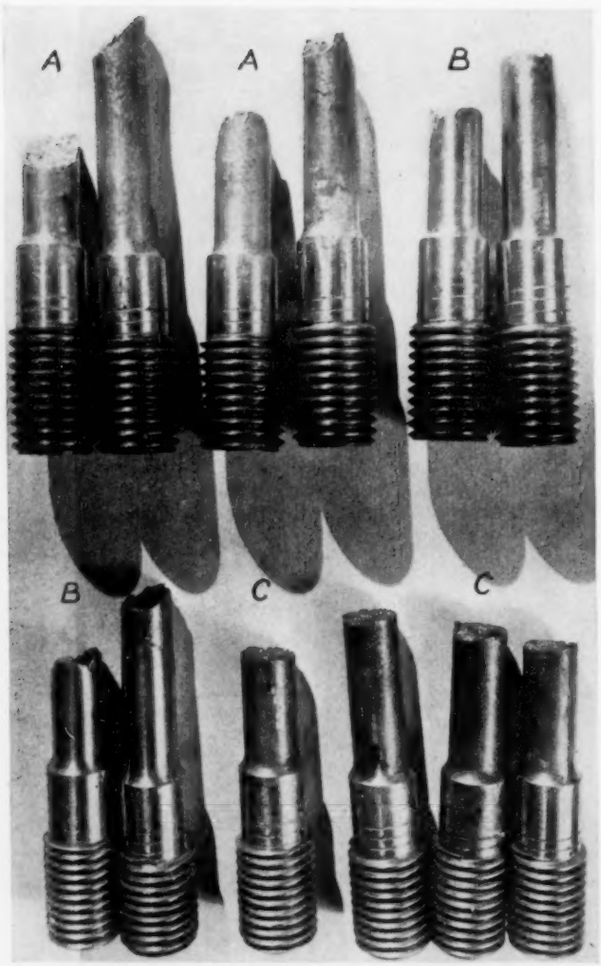

Fig. 14—Fractures in Low-Strength Welds. A—At Room Temperature. B—At −15° C. C—At −80° C.

had increased strength and ductility at −80° C.—the properties at this temperature (nearly 70,000 psi tensile strength, 15% reduction of area, 10% elongation in 2 inches) being exceptionally good. The impact tensile properties of the high-strength bronze welds were also better than the Naval bronze welds, but the impact tensile strength and ductility of both welds unexpectedly decreased slightly at the lower temperatures.

Austenitic Stainless Steel.—The static and impact tensile properties of the unwelded 18–8 used in the present investigation are shown in Fig. 8. The static tensile strength increases steadily with decrease in temperature within the limits of the tests, but the ductility decreases only between +20 and −20° C., below which the ductility does not change appreciably. As noted last year,[1] the impact tensile properties are remarkably good and do not materially change with temperature.

The arc welds, Fig. 9, have about the same tensile characteristics as unwelded base metal. The maximum static tensile strength of the arc welds at +20 and −80° C. is only a few per cent below unwelded base metal. The ductility is satisfactory, the difference between welded and unwelded being about the same throughout the range of temperature under investigation. The static tensile results for the oxyacetylene welds, Fig. 10, are decidedly inferior, although the impact tensile strength is nearly as good as that of the arc welds. The microstructure of the gas welds consists of equiaxed grains with little indication of dendritic structure, Fig. 11. The metal arc welds, however, were dendritic in structure, Fig. 12. In neither weld were there appreciable amounts of fine carbide or porosity.

Static Tensile Tests of Oxyacetylene Welds.—An investigation was made of the static tensile properties at low temperatures of oxyacetylene welds in a low-carbon steel using low-strength and high-strength rods. The comparison of the rods is shown in Table 1.

Table 1—Comparison of Oxyacetylene Welding Rods and Plate Metal

	C	Mn	Si	S	P
Plate metal	0.28	0.57	..	0.022	0.016
Low-strength rod	0.06	0.20	0.01	0.03	0.02
High-strength rod	0.18	1.00	0.40	0.03	0.02

The results in Table 2 show that the high-strength welds have nearly the same strength and ductility as the unwelded base metal. The high-strength welds fractured in the base metal whereas fracture of the low-strength joints occurred in weld metal. Figures 13 and 14.

Table 2—Average Static Tensile Properties of Plate Metal

(0.28 C) and Low-Strength and High-Strength Single "B" Welds. (0.505 inch diameter tensile specimens)

Temperature	Tensile Strength psi			Elongation % in 2 Inches			Reduction of Area %		
° C.	B	L	H	B	L	H	B	L	H
24	58,200	43,500	56,200	38	14	23.5	54	23.5	53
0	61,400	42,300	55,300	..	7	10	54.5	7.8	16.5
−15	62,000	42,500	60,300	38	11	26	54	15	61
−80	70,000	45,600	70,800	39.5	1.5	27	49.5	1.6	56.5

B—Unwelded base metal
L—Low-strength weld
H—High-strength weld

(Fractures of the low-strength weld metal are not indicative of high-quality welds.)

Acknowledgments

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An Alternating Current Non-Destructive Test for Welded Seams*

By W. B. KOUWENHOVEN† and A. E. VIVELL‡

WELDING offers many advantages, but these are partially offset by the occasional erratic work of welders or welding machines. The majority of welds are satisfactory but the occasional flaws may be of appreciable size. Non-destructive methods for testing welds are, therefore, of great importance. None of the present-day methods are entirely satisfactory. This paper describes a new method for the detection of flaws in welds.

In this method the welded seam is magnetized by a strong alternating magnetomotive force. Two search coils connected in opposition are placed on the seam so that any stray field which may be present cuts their windings. The wave of the electromotive force induced in the coils is observed by means of an oscillograph. Tests have shown that, under the proper conditions, the presence of flaws in the weld produce unique changes in the shape of the emf. wave. These changes may be easily recognized and the defective part eliminated.

Apparatus

The magnet used in this investigation had a U-shaped laminated core of six square inches cross section. The distance between the poles was seven inches. The magnetizing winding consisted of 1000 turns of No. 10 B and S gage wire.

The exploring or search coils were each of 1500 turns of No. 36 B and S enamel cotton covered wire. They were wound on bakelite spools having a diameter of three-eighths of an inch. The outside diameter of the spool with the winding in place was three-quarters of an inch. The bakelite flanges on one end of the spools were one-sixteenth of an inch thick and on the other end one-eighth of an inch. The spools were placed side by side and bound together with friction tape. They were connected differentially and used as a unit.

The resultant emf. set-up in the differential coils was observed by means of a cathode ray oscillograph. The oscillograph used was an R.C.A. type TMV-122-B, having a built-in amplifier and sweep circuit. The tube had a three-inch diameter fluorescent screen designed for visual observation. This oscillograph performed very well in our visual tests and we also made a number of photographs of the wave shapes. These photographs were taken on verichrome film with an exposure of about 50 seconds. The results were quite good when the lines were all of the same thickness and the waves remained stationary on the screen. In actual practice the wave forms never remained completely stationary and none of the sharp peaks photographed well. Visual observations were on the whole much more satisfactory than the photographs.

Operation

The specimen under test was placed on the poles of the magnet and the alternating magetic field applied. The sample was not clamped to the pole faces. The mmf. applied was sufficient to saturate the material under test and 100 or more oersteds were used. The frequency used depended upon the thickness of the specimen. A frequency of sixty cycles was used for thin specimens and 25 cycles or less for thicker materials.

The differential coils were moved along the welded seam and the wave shape of the resultant emf. observed on the cathode ray oscillograph screen.

The emf.'s set-up in the search coils are due to two sources. Those caused by the leakage field are of the same sign in both coils and are cancelled out by the differential connection. Any reduction in the effective cross section of a specimen produces a localized stray field at the surface because of the high degree of magnetic saturation employed. These localized fields are also a source of emf. The emf.'s that they induce in the search coils are of opposite sign and owing to the differential connection these reinforce each other. The resulting wave of emf. observed in the oscillograph is, therefore, mainly due to the presence of the localized stray fields set up by defects in the specimens.

In the preliminary tests the welded seam under study was kept midway between the poles of the magnet. With the seam in this position and the coils on the seam the wave shape produced by good welds showed a small but definite peak. If the seam was defective the peak was very much sharper and more pronounced. This is clearly shown by the photographs of the waves taken of one good weld, specimen W-1-D and two poor welds, specimens W-5-B and W-5-H, Fig. 1 with the weld midway between the poles. The applied mmf. equaled 150 oersteds and its frequency was 60 cycles.

When operating with the seam centrally located between the poles of the magnet it was found necessary to measure the height of the peaks in order to differentiate between good and poor welds. This proved to be a disadvantage, especially where small flaws were present.

It was found, however, that when the weld seam under test was approximately three-quarters of an inch from one of the poles of the magnet that the wave shape of the emf. induced in the coils by a good weld differed markedly from that of a poor weld over a wide range of the applied mmf. This may be clearly seen by referring to Fig. 2 which shows the wave shapes for the three specimens of Fig. 1 when the seam was placed at the side near one of the poles of the magnet. The frequency and the strength of the magnetic field were the same in both tests.

This "side test" or location of the seam under test magnet pole was adopted as the standard test method. It gave results which made it simple to differentiate between good and poor welds, and eliminated the necessity of measuring the height of the peaks.

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Specimens

The welded specimens studied were cut from 12 x 12 x 0.25 inch and 12 x 12 x 0.5-inch flange steel plates which had been formed by electrically welding together two 12 x 6-inch plates. The cut specimens were two inches wide and 12 inches long. All the welds were 60°, double "V" butt welds, with the welders bead on both sides. The plates were welded and furnished through the courtesy of the Bartlett Hayward Company. Half of the specimens were good welds and the other half showed porosity and lack of fusion. All of the good seams were very good and all of the poor ones were very poor.

The desirability of having a welded specimen with a known flaw of a definite size at a given location was recognized. A number of attempts to produce such specimens by welding proved futile, and efforts were made to simulate defects by drilling holes of various sizes in plates and bars. The attempts to build up a specimen in which the defect was readily controllable led to the development of a laminated specimen. In these specimens sheet or laminated material was used. In one of the laminations a narrow slot 0.01 inch wide was cut through the sheet at right angles to its long axis. This slot or defect did not extend to the outer edges of the lamination but ended about one-eighth of an inch from each edge. The particular lamination containing the defect could then be assembled in any desired position in a stack of good laminations. The percentage of defective area in the specimens could be changed by varying the size of the slot or the number of strips in the bundle.

The surfaces of the laminations were clean and smooth and when assembled in a bundle they were either sewn together by spot welding or tightly clamped. In these laminated specimens it was possible to place the defect (slotted lamination) at the surface, or at any desired depth beneath the surface. These laminated specimens proved very satisfactory and tests made with them are reported in another paper.

Fig. 1—Center Tests on Smooth Specimens f-60 H-150

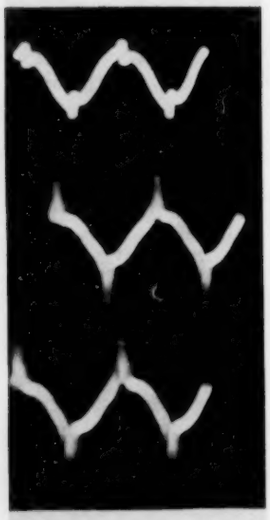


Fig. 2—Side Tests on Smooth Specimens f-60 H-150

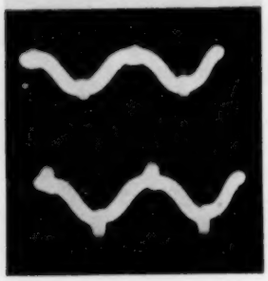


Fig. 3—Side Tests on Rough Specimens f-60 H-200

Results:

Experiments showed that the wave shape was independent to a considerable extent of the distance between the coils and the surface of the specimen under test. A change from one-sixteenth to one-eighth of an inch in this distance reduced the height of the peak of the wave on the oscillograph by about 12 per cent.

Tests made of welded seams with the bead in place showed that it was possible to detect defective welds without first machining or grinding off the bead. The waves obtained with two of the rough unmachined specimens are shown in Fig. 3. Specimen W-5-E was a poor weld, and specimen W-1-B a good weld.

A considerable number of welded samples were tested magnetically and some of these were tested to destruction in accordance with A. S. M. E. "Code for Unfired Pressure Vessels." In preparing the specimens for the tension tests the bead was machined off in accordance with the A. S. M. E. specification. Some of these specimens were tested magnetically by the differential coil test in the rough state with the bead in plate and also after the bead had been machined off. The results of the tension tests and of the magnetic tests for a number of specimens are given in the table below.

Table I—Test Data. Welded Specimen

Specimen Number	Magnetic Data for Side Test Height of Peak Observed on Oscillograph Screen	Tension Tests Location of Break	Tension Tests Lb./Sq. In.
W-1-B	Rough seam showed no peak. (See Fig. 3)	Not tested	....
W-1-D	Rough seam gave a 1 mm. peak. When smooth no peak present. (See Fig. 2)	In plate	68,400
W-2-A	Smooth condition gave a 1 mm. peak	In plate	65,600
W-5-B	Rough seam gave 7 mm. peak. When smooth a 10 mm. peak. (See Fig. 2)	In weld	22,600
W-5-E	Rough seam gave a 4 mm. peak. (See Fig. 3)	Not tested	...
W-5-H	Rough seam gave 6 mm. peak. When smooth a 10 mm. peak. (See Fig. 2)	In weld	15,900
W-7-A	Smooth seam gave 6 mm. peak	In weld	25,100
W-8-B	Smooth seam gave 7 mm. peak	In weld	28,800

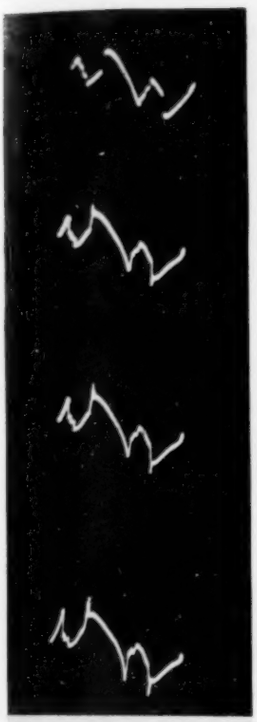


Fig. 4—Side Tests on Laminated Specimens I-60 : H-200

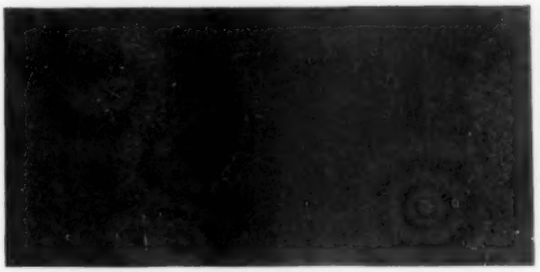
Fig. 5—Side Tests on Laminated Specimens I-60 H-200

It is evident from these results on welded specimens that the good welds were very good and that the poor ones were very bad.

In order to study further the possibilities of the new method, the laminated specimen was used. The results for two laminated specimens made up of different materials are given below in Figs. 4 and 5.

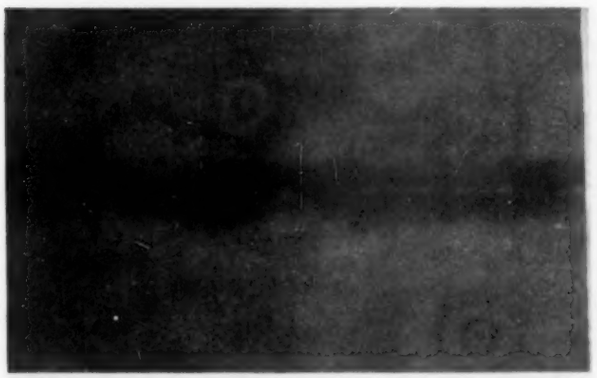
The specimens shown in Fig. 4 consisted of ten laminations of 12 mil. thick, 1.5 inches wide, 12 inches long strips of electric sheet steel. For purposes of comparison one specimen No. 54 was made up entirely of good laminations and the other three specimens contained a flaw which was produced by cutting a slit 0.01 inch wide one-half way across a lamination. This slotted lamination, when included in a stack of ten laminations, introduced a five per cent flaw. The effects of this small flaw on the wave shape are shown for the flaw at different distances below the surface. Specimen 50 shows the change in the wave shape that is produced when the strip containing the flaw is the second strip below the surface in the stack of ten good laminations. The sharp peaks are definitely marked. On specimen 51 the flaw is the fourth strip from the surface and in specimen 52 it is the sixth. This was the greatest distance below the surface that the five per cent flaw could be detected by the differential coil method.

Specimens 40 to 42, Fig. 5, were made up of strips of black sheet iron each 0.03 inch thick. They were 1.75 inches wide by 12 inches long. A slot 0.01 inches wide was cut in one lamination at the center about $^1/_2$ way across. The comparison specimen 43 made up of ten good laminations of this material gave the top wave trace of



W-1-D
(good weld)
Fig. 6—Smooth Specimen

Fig. 7—Rough Specimen
Plate W-5 (poor weld)



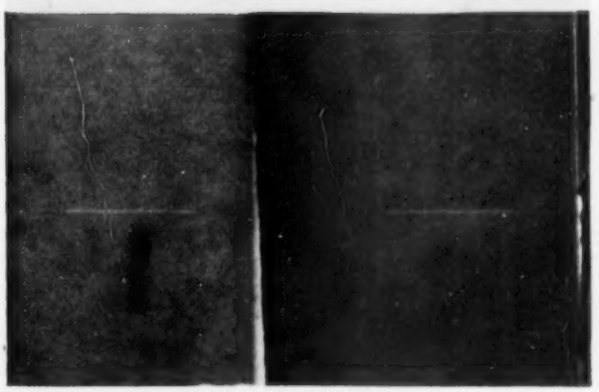


Fig. 8—Laminated Specimens
Specimen #60 Specimen #61

Fig. 5. When the defective lamination was placed in the stack at the second from the top, its presence caused a marked change in wave shape as shown for specimen No. 40. Specimen No. 41 contains the five per cent flaw in the fourth lamination from the top of the bundle, and in specimen No. 42 is the sixth. The change in wave shape can still be detected at this depth. Below this, however, the five per cent flaw could not be detected.

In these two specimens the laminations were cleaned and polished before being clamped together. The tests were made at 60 cycles and a mmf. of 200 oersteds was used. The surface of these specimens was smooth.

X-ray Pictures

X-ray photographs were taken of a number of specimens. These photographs confirmed the results of the magnetic and physical tests, and only three of them are reproduced here.

The X-ray picture of specimen W-1-D in its smooth state is shown in Fig. 6. It is clearly evident that the weld is excellent and contained no flaws. Fig. 7 gives the X-ray picture of welded plate W-5 from which specimens W-5-B, W-5-E and W-5-H were cut. This exposure was made with the bead in place, and shows evidence of a poor weld. The results of the tension tests reported in Table 1 check the X-ray photographs.

Two X-ray pictures of a laminated specimen containing a five per cent flaw are shown in Fig. 8. In the left-hand picture the lamination containing the slit or flaw was the fourth from the surface of the bundle and in the right-hand one it was the seventh strip beneath the surface. As would be expected the X-ray pictures are identical for both locations, and show the flaw clearly.

Conclusions

In testing long seams, the differential coils and the magnet may be held stationary and the piece under test moved or the material may be held stationary and the magnet and test coils moved. The material under test will heat up rapidly due to eddy currents set up by the powerful alternating field. This, however, does not act as a disadvantage as the increase in temperature raises the resistivity of the material and reduces the eddy currents which permit better flux penetration.

When the steel is one-quarter inch or less in thickness, alternating fields of 25 or 60 cycles give satisfactory results. For half-inch specimens 25 cycles should be used. Above one-half inch lower frequencies give best results. The flux penetration can be calculated by the use of Steinmetz's equations. It should be kept in mind, however, that at the mmf.'s used the permeability is less than 100.

The following conclusions may be drawn from this work:

1. The use of an alternating magnet field provides a method for studying welded seams with the bead in place and eliminates the necessity of machining the specimen before testing.
2. The change in the wave shape of the emf. induced by the presence of a flaw is a definite indication of the quality of the weld.
3. For best results magnetic saturation is desirable and the frequency should be low enough to insure penetration of the magnetic flux.
4. The laminated specimens developed provide a simple and flexible test specimen for the study of welds.

Acknowledgment

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Studies of the Oxyacetylene Cutting Process*

By H. R. BULLOCK†

MODERN cutting apparatus, although highly developed, still leaves something to be desired in many instances. The possibility of improvement in the process appears to lie in two directions. First, an increase in efficiency and second, an improvement in precision with a definite possibility that both of these properties are interdependent.

The variables in a cutting process are so well known that it seems unnecessary to mention them. Studies of the relation of speed and pressure to material thickness, using commercial nozzles, have been made over a period of two years and results of these studies indicate that with few exceptions the manufacturers specifications as to speed and pressure are substantially correct. By suitable refinements it is possible to obtain better than standard cuts, but in almost every case it would be difficult to apply these refinements industrially.

In view of the foregoing conclusions it was felt that further improvement in the cutting operation could only be made by an improvement in the cutting nozzle itself, aided possibly by mechanical refinements in the blowpipe.

Study of the action of various types of nozzles was divided into two categories. First, the study of the oxygen stream issuing from the nozzle and second, the actual cutting operation.

Study of the form of the oxygen stream was first attempted by the Schlieren method. It was suspected from examining almost perfect cuts that raggedness was the result of a periodic stream disturbance of extremely high velocity. Photographic difficulties due to Schlieren optics prevented the recording of any worth while data even when extreme refinements were made. In spite of the expense and labor in constructing the apparatus the Schlieren system was discarded.

Silhouette or shadow photography seems to be the best method of study. A characteristic photograph of the stream is shown in Fig. 1.

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† Instructor in Mechanical Engineering, Mass. Inst. of Tech.

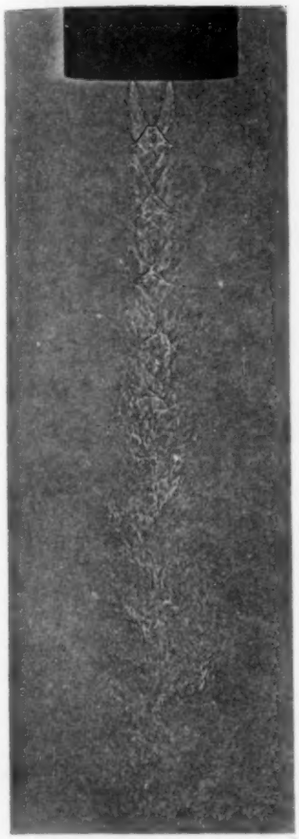

Fig. 1 Silhouette Picture of Oxygen Stream Taken at High Velocity

Figure 2 shows the change occurring in several successive frames (speed 200 frames/sec.). Although the total change is shown quite clearly, the manner in which the change takes place is not discernible at this comparatively slow camera speed. Apparatus is now being assembled to extend the speed to a possible 1000 frames/-sec.

Since the definition in such photography requires a point source of light, it was necessary to develop special spark gaps. Figure 2 was taken with the gap only eighteen inches from the film.

Figure 3 is a schematic diagram of this apparatus and Figs. 4 and 5 are photographs of the apparatus as used for both still and motion pictures. The blowpipe and nozzles illustrated here were developed especially for this research.

In an attempt to correlate the studies of the oxygen stream with actual cuts, the cutting operation was investigated with high-speed motion pictures. The camera used was the ERPI, manufactured by Electrical Research Products, and loaned to the Institute through the courtesy of Lever Brothers Company. In this camera a prism rotating at high speed acts as a shutter and at the same time permits continuous film motion. Prior to using the high-speed camera a careful study of filters was made with an ordinary motion picture camera and a speed of only 16 frames/sec. By suitable shutter-slit and aperture adjustment, the exposure at the slow speed was made the same as the theoretical exposure at 1000 frames/sec. Figures 6 and 7 show single frames of films taken with and without filters. It will be noted from Fig. 7 that by superposing high intensity light having a narrow band of color and using a filter which transmits only this band, it becomes possible to photograph the molten metal and slag by reflected rather than transmitted light. This possibility becomes very important in view of later developments.

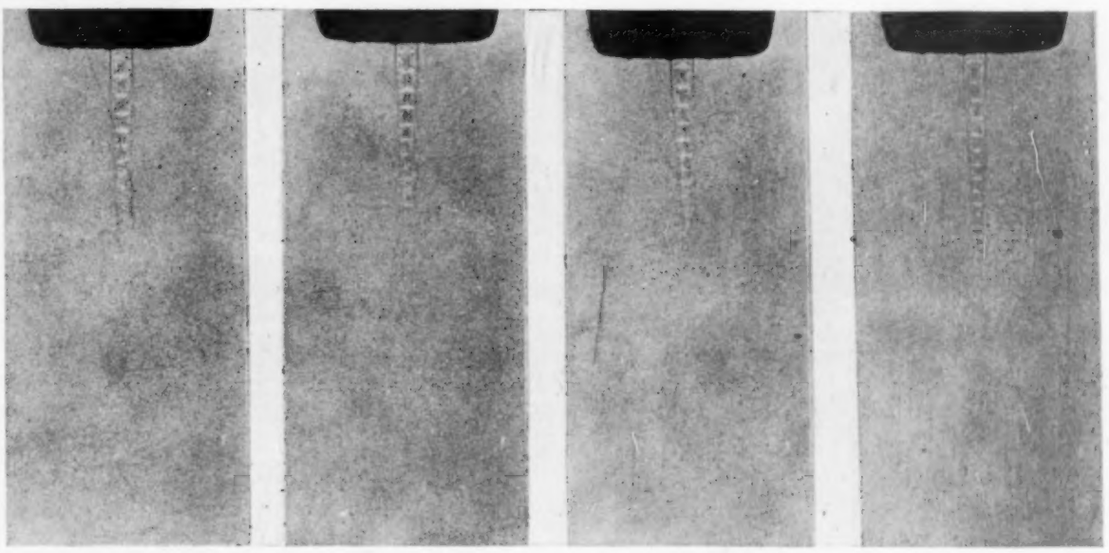

Fig. 2

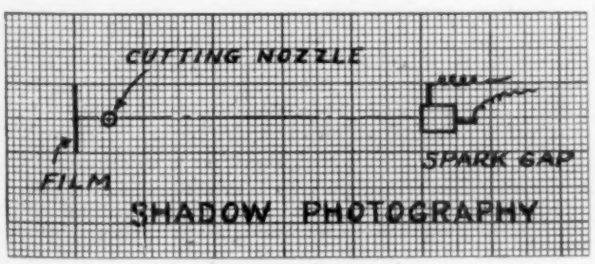

Fig. 3

Pictures taken with the high-speed camera were made and examined by projection. A general view of the equipment is shown in Fig. 8. No prints of the film are submitted because of the impossibility of reproduction. In examining the film by projection it is apparent that there is a periodic disturbance of high velocity which starts near the top of the cut and is magnified in amplitude as the reaction approaches the bottom. The rate

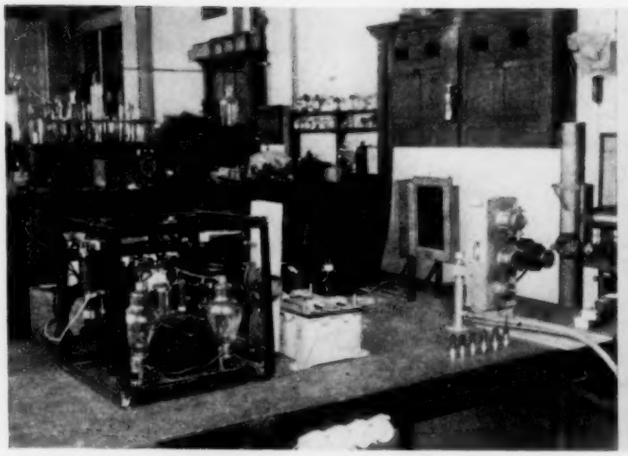

Fig. 4

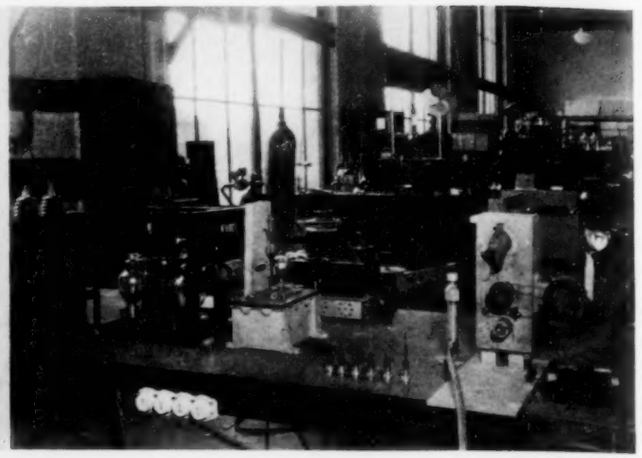

Fig. 5

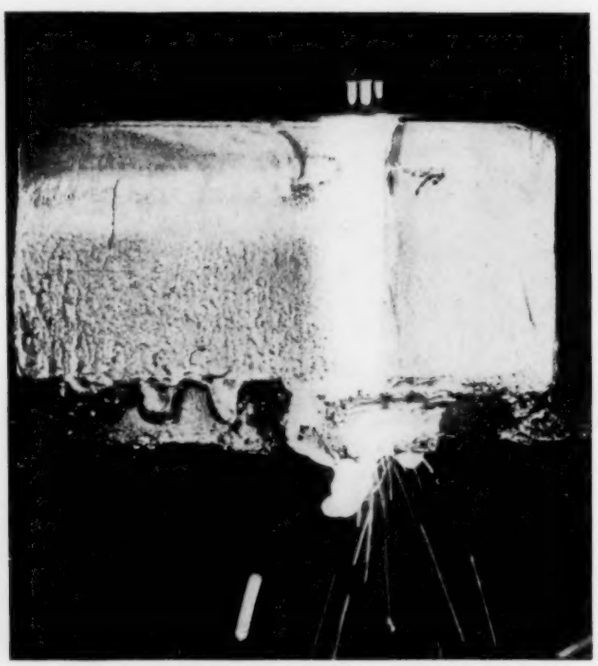

Fig. 6

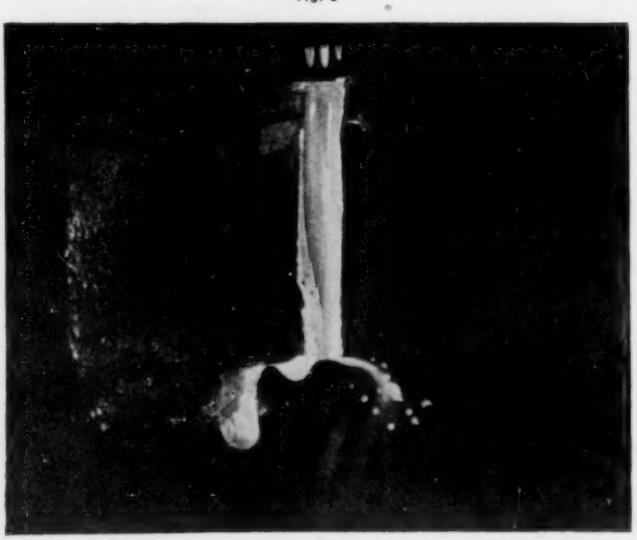

Fig. 7

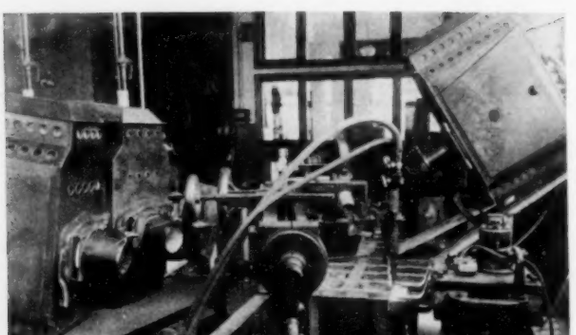

Fig. 8

of progress of this disturbance seems to be about 150 ft./sec.

In so far as any definite conclusions are concerned the work done up to date has proved nothing. Even at the highest camera speed (approximately 1200 frames/sec.) the velocity of the disturbance is too high for any careful analysis, and it has become necessary to devise means of increasing camera speed to two or three thousand frames per second. This is now being undertaken.

Despite the lack of conclusive evidence which would enable definite conclusions to be made, it becomes possible to make reasonably intelligent conjectures as to the mechanism of the turbulence. Apparently, a mechanical shock impulse in the nozzle (which seems to be periodic) disturbs the velocity of the reaction between the oxygen and the iron. Then, due to the resulting change in heat, the disturbance is greatly magnified and becomes greater as the bottom of the cut is approached.

The work being done at present consists of further development of the photographic apparatus and technique in both fields and the study of internal flow in the nozzles.

Development of photographic technique has been largely due to Mr. Herbert E. Grier of the Institute staff and without his assistance this work would have been impossible. The author also wishes to acknowledge the courtesy of Lever Brothers Company in loaning us the camera with which the cuts were photographed and to thank Dr. Harold E. Edgerton for his timely advice and assistance at awkward moments.

The Heat Effect in Welding*
A Review of the Literature to January 1937

By WALTER H. BRUCKNER†

SUMMARY

FORTY references have been cited in an author review of the heat effect in welding. A subject review covering data on all the variables in welding, which determine the heat effect, is not possible at the present time due to the lack of sufficient accumulated literature specificially devoted to this subject.

The conclusions from the investigations of the authors cited are summarized as follows:

A. Thermal Cycle of the Welding Operation

1. The rate of application of heat to the parent metal through the weld metal (and the arc or gas flame) is lower for gas welding than for arc welding. The heat source in the latter case is more intense, and therefore produces a steep thermal gradient compared with gas welding.

2. The extent of the heat disturbed area in the parent metal decreases in the following order, bare rod gas weld, heavily coated electrode arc weld, bare wire arc weld. The cored and the austenitic electrodes produce a smaller area of heat disturbance than the heavily coated electrode, but their relationship to bare wire arc welds has not been defined.

3. The extent of the heat disturbed area for arc welds is affected by the following factors; the area is increased by

(*a*) decreased speed of welding (current constant)
(*b*) increased current (speed constant)
(*c*) decreased arc length (energy constant)
(*d*) decrease in mass of parent metal
(*e*) increase in preheat or removal of chill
(*f*) use of thermal insulator for backing of weld.

For arc welds the extent of the heat-disturbed area is directly related to the electrical energy input which may be expressed as Joules per inch of weld.

Penetration for arc welds as measured by the depth of the fusion line below the welded face does not necessarily increase with the increased heat-affected area since the area usually increases in width.

4. The quench produced in the overheated region of the heat disturbed area by abstraction of heat into the surrounding mass is more drastic the smaller the area of heat disturbance, the mass of the welded section remaining constant. There apparently are exceptions to this rule in the case of cored and austenitic electrode-arc welds.

B. Hardenability

1. The maximum hardness produced in the welding operation is greater for arc than for gas welding the hardness progressively across the joint is directly related to the thermal gradient produced by the welding process. The factors which effect an increase in the heat-disturbed area also effect a decrease in maximum hardness and in the slope of the hardness gradient across the joint.

2. A direct relationship has been found to exist between hardenability and cracking in the heat-disturbed area for arc welds. The presence of martensite near the fusion line of a weld is usually associated with fissuring and cracking.

3. Investigations of arc welds on a series of plain carbon steels indicate that up to 0.35% carbon the hardenability increases with carbon content in a smooth curve which is practically a straight line. Above 0.35% and up to 0.45% carbon there is a slightly increased rate of hardenability with carbon content, but still no sharp break in the curve anywhere between 0.15% to 0.45% carbon.

4. The alloy contents of steels for welding intensify the hardening effect of carbon, but the latter is most effective in increasing the hardenability. For this reason attention has been given to low alloy steels in which the carbon is kept low to prevent excessive hardening and the alloy addition made to increase strength.

C. Physical Properties

1. Where the hardening has not been excessive such as in the gas and arc welding of plain carbon steels below 0.25% carbon the heat-disturbed area has good ductility in cold bend, tensile and shear tests.

2. The use of the notch impact test appears to be the most sensitive gage of local disturbance in the metal of the heat affected area. Whereas the hardness tests of arc welded plain carbon steels show a gradual increase in hardenability with carbon, the notch impact tests indicate a sharp reduction in toughness of the heat disturbed area in arc welds at a carbon content of approximately 0.30% and beyond.

The information available on notch impact tests of carbon steels where the notch was placed in the heat-disturbed area after arc welding indicate that plain carbon steels below 0.25% carbon do not suffer a loss of toughness. The data on gas welds are not as reassuring since the production of a large grained, Widmanstaetten structure appears to promote a considerable loss of toughness in the heat disturbed area.

D. Welding Tests

1. Procedures for making arc welding tests to determine the comparative effects of welding speed, current, mass and metal composition have been established. These procedures consist of surveying the cross section of a plate upon which a single bead has been deposited with the metallic arc, the conditions of deposition and plate composition varying as desired; with the electrode, arc height, etc., kept constant. The survey of the cross section consists of indenting the polished section with a pyramidal indenter (Vickers type) using a 10 kilogram

* Paper to be presented at Annual Meeting, AMERICAN WELDING SOCIETY, Atlantic City, October 18–22. Contribution to Literature Division of Welding Research Committee.

† Naval Research Laboratory, Anacostia Station.

load. The indents are spaced at 0.02-inch intervals in the overheated region of the disturbed area and at 0.05-inch intervals into the undisturbed parent metal. Such a survey provides a hardness contour from which values of maximum hardness, hardness gradient, extent of heat disturbance into parent metal may be obtained.

2. Similar procedures for comparative tests of gas welding have not been reported in the literature.

3. Quenching tests have been cited in the literature in which parent metal is brought to temperatures above A_3 and quenched, the material being then subjected to bend tests. Such tests appear to depend upon the thickness of the materials which are tested. Other tests in the German literature are intended to permit the determination of welding sensitivity by means of timed quench tests in oil and water. There should be little change in mechanical properties (tensile, yield strength, elongation, impact) for a non-sensitive welding steel.

The results obtained in these quench tests cannot be checked by an investigator since no definite information as to sample size and other conditions has been included in the literature.

4. The production of synthetic heat-disturbed metal by means of heat treatment at high temperatures of small masses of the parent metal has been successful for mild steel of around 0.25% carbon. The overheated metal of the weld joint has been reproduced for both gas and arc welding.

The literature on these successful investigations provides details of the procedure for synthesizing gas weld heat disturbed metal; the procedure for arc welds has been omitted from the only literature source on the subject.

The tests of impact, tensile, fatigue values reported for the synthetic materials indicate a fruitful source of information is thus made available for determining properties of the heat-affected area. Similar tests spread over a large number of compositions with varying carbon, alloys, etc., and checked with the behavior of actual welds of the same compositions during welding can be expected to provide some valuable comparisons.

E. Remedies for the Heat Effect

1. Adjustment of steel composition by reduction of carbon-content and increase of alloy has been a practically unanimous conclusion from welding tests and experience throughout the literature. The setting of a maximum limit for the carbon, below which a steel of easy weldability is obtained has not been definite since a general statement cannot include all the variations in alloy contents that appear in commercial steels. At various times the carbon limit has been set at 0.25%, 0.18% and even 0.14% for a fool-proof welding steel.

2. The use of preheat to reduce the welding-quench of steels of higher strength, thus reducing the maximum hardness of the heat-disturbed area and the tendency for crack formation; has proposed either as a local preheat with the gas torch on both sides of the scarf or as a general preheat of the entire section to be welded. The literature, however, does not report any extensive investigation of the character of the heat disturbed area when preheat is used, except in the case of some very special steels.

3. The use of stress relief of the welded joint at temperatures of 1100 to 1250° F. to increase the safety of the joint has been reported in the literature. The incidental decrease in maximum hardness of the heat-disturbed area has been noted and the treatment suggested as a remedy for excessive hardness. However, the existence of excessive hardness is generally coupled with existence of fissures which are not remedied by stress relief.

F. Required Information in the Study of the Heat Effect in Welding

1. The literature reports a number of results of notch impact tests of the heat-disturbed region of welds. The tests are made by cutting the notch out of the area in the neighborhood of the fusion line. Obviously this type of test gives a result based on the material under and surrounding the notch, e.g., a volume of deformed metal which is heterogeneous and which by excluding from the test the metal cut out to form the notch may have excluded the most critical area.

Some correlated tests are needed to explore the reliability of the above notch impact tests of welded joints. Such tests are visualized as the exploration of the heat-disturbed area and synthetic reproduction of these areas in impact specimens. A comparison of the impact properties of these specimens with the results of impact tests on actual welds where the notch is made in the heat-disturbed area would improve our knowledge as to the most critical area of the heat-disturbed area and would provide a gage as to the reliability of results of the latter impact test.

2. The entire field of carbon-alloy combinations in steels needs to be charted to determine carbon/alloy ratios such as carbon/manganese, carbon/nickel, carbon/nickel-copper, carbon/molybdenum, etc., and the corresponding maximum hardness levels in welding, in order to indicate undesirable limits, most ineffective alloy additions for hardening during welding with the largest contribution to the materials' strength.

3. There is also lacking actual values of the maximum hardness and other properties in the heat-affected area around the first bead laid in the groove of multiple pass welds of various thicknesses. Such values are needed for comparisons of single bead welds made by laying a bead on flat plates.

The question that arises in connection with the standard arc-welding tests of beads deposited on standard $^1/_2$-inch thick plates is: how is the maximum hardness found, related to the hardness and other properties of the first bead-heat-disturbed area in welding various thicknesses of material.

There also needs to be determined the effect of stress concentration in promoting crack formation in the first bead-heat-disturbed area and whether this has been disregarded in setting up standard welding tests of weld deposits on flat, $^1/_2$-inch thick plates and basing comparisons on maximum hardness obtained.

Introduction

The review was restricted to a search for material in the English, French, German and Italian languages on studies of the heat effect due to fusion welding with the arc and oxyacetylene torch. Studies on special steels, cast metals and non-ferrous materials have been passed up in favor of publications containing data on structural steels, since the latter receive, by far, the largest welding application.

All the important literature which reported a specific investigation of thermal effects in the above catagories is believed to be assembled here. Occasionally a reference was made in some of the papers to investigations of special interest for the review, but which were unavailable to the reviewer. Such references are given in the appended supplementary bibliography.

The Heat Effect in Welding

Portevin, A. and Séférian, D. The Thermal Study of Welding. *Chaleur et Industrie*, **16**, No. 185, 409–424, Sept. 1935.

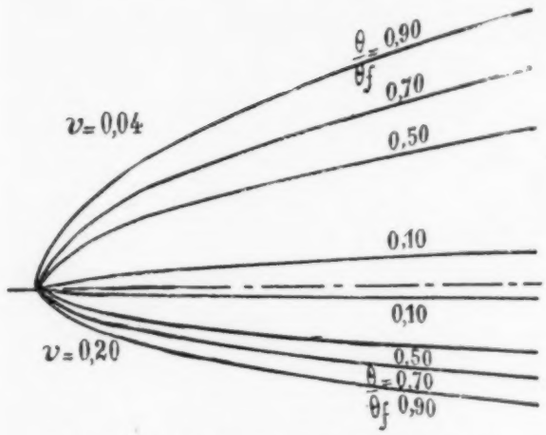

Fig. 1—Effect of Speed of Welding on Distribution of Isotherms

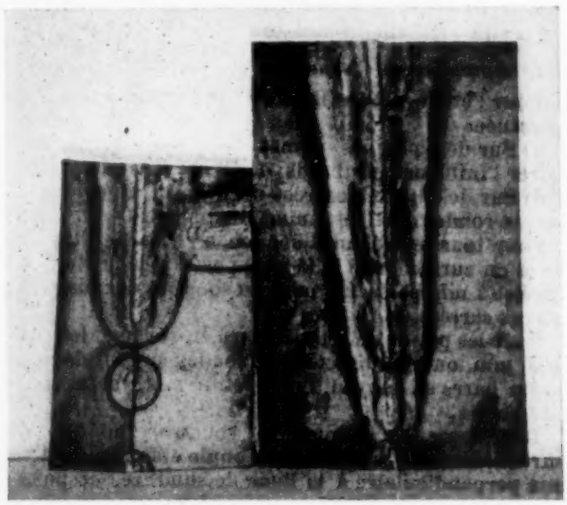

Fig. 2—Isothermal Envelopes Observed for Different Practical Cases of Fusion

The welding profession is particularly fortunate in having two such authors, with a command of both metallurgy and the complex mathematics of the heat effect, undertake this study and provide the mathematical tools required to predict the extent of the thermal disturbance. This publication heads the review, since it is the reviewer's opinion that it is the most important contribution that has been made in this field. With it, welding becomes a science, its effects mathematically predictable. The complete translation of this publication, made by the reviewer, is expected to be made available for publication in the near future.

The first part of the publication is a review of previous attempts to define the thermal effects by means of mathematics and experimental procedure. The authors' development of the heat equations and application to the welding processes in gas and arc-welding of steels and other metals occupies the entire first part of the publication. The mathematics has enabled the authors to extrapolate some of the experimental values obtained, which are reported in the second part of the paper.

The usefulness of the predictions is cited for the case of the distribution of the isotherms for different speeds of welding, the curves of Fig. 1 showing the calculated values for two speeds. Figure 2 shows the isotherms indicated by temper colors as is familiar to any one who has inspected a weld. The relative effects of increased speeds of welding are seen to be predictable, and the extent of the heat-affected area required to reduce the heat effect

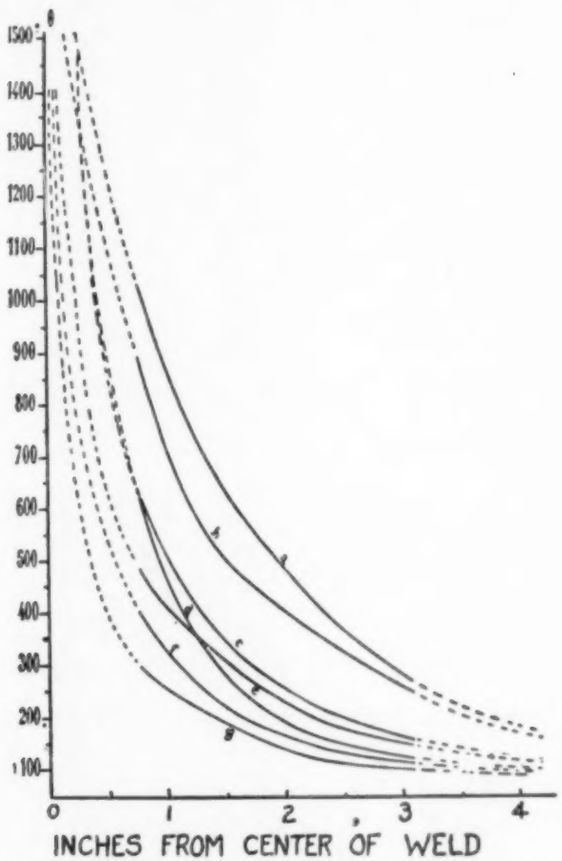

Fig. 3—Maximum Temperature Reached Along a Line Perpendicular to a Forehand Oxyacetylene Weld in Mild Steel

θ = temperature in ° C.
(a) 0.39 inch plates, couples at mid-thickness
(b) 0.39 inch plates, couples on surface
(c) 0.39 inch square bars, couples at mid-thickness
(d) 0.20 inch plates, couples on surface
(e) 0.39 inch square bars, couples on surface
(f) 0.39 inch diameter round bars, couples on surface
(g) 0.20 inch diameter round bars, couples on surface. Portevin and Séférian[1]

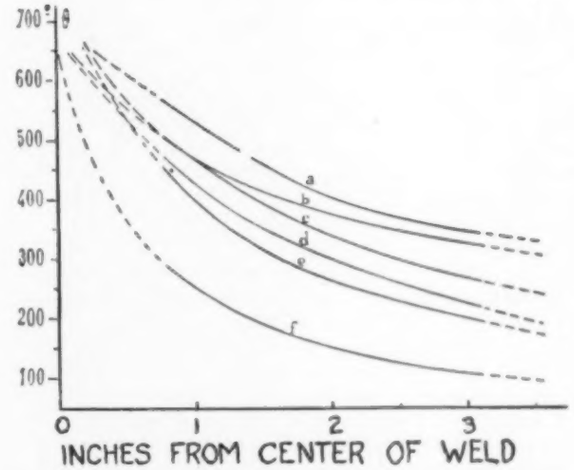

Fig. 4—Maximum Temperature Reached Along a Line Perpendicular to a Forehand Oxyacetylene Weld in Aluminum

(a) 0.39 inch plates, couples at mid-thickness
(b) 0.39 inch plates, couples on surface
(c) 0.39 in sq. bars, couples at mid-thickness
(d) 0.20 inch plates, couples on surface
(e) 0.39 inch square bars, couples on surface
(f) 0.20 inch diameter round bars, couples on surface. Portevin and Séférian[1]

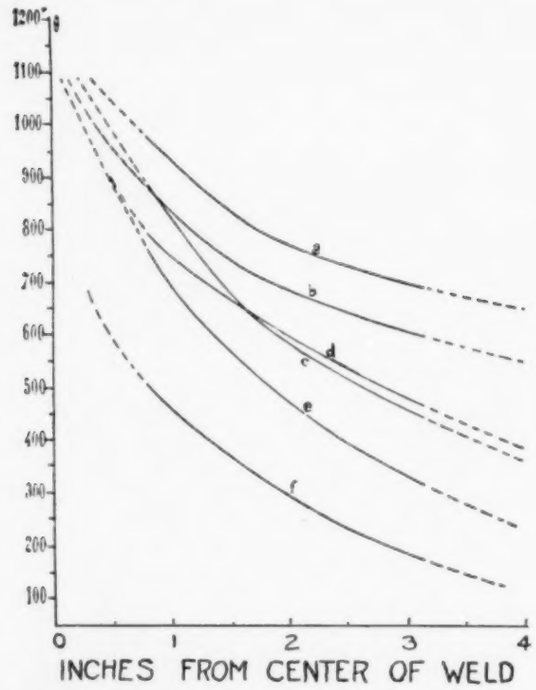

Fig. 5—Maximum Temperature Reached Along a Line Perpendicular to a Forehand Oxyacetylene Weld in Copper

(*a*) 0.39 inch plates, couples at mid-thickness
(*b*) 0.39 inch plates, couples on surface
(*c*) 0.39 inch sq. bars, couples at mid-thickness
(*d*) 0.20 inch plates, couples on surface
(*e*) 0.39 inch sq. bars, couples on surface
(*f*) 0.20 inch diameter round bars, couples on surface. Portevin and Séférian[1]

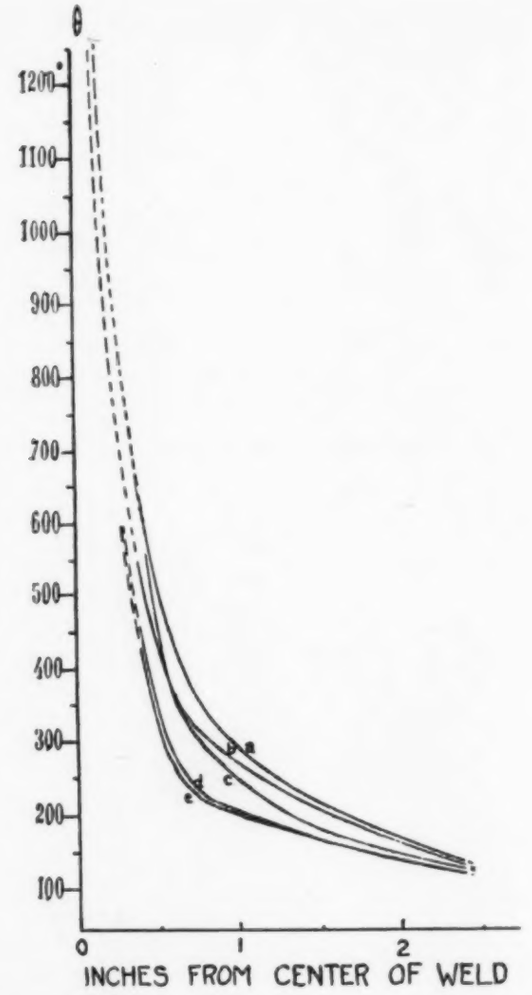

Fig. 6—Maximum Temperature Reached Along a Line Perpendicular to a Covered Electrode Weld in Mild Steel

θ = temperature in ° C.
(*a*) 0.39 inch plates, couples at mid-thickness
(*b*) 0.39 inch plates, couples on surface
(*c*) 0.20 inch plates, couples on surface
(*d*) 0.39 inch square bars, couples at mid-thickness
(*e*) 0.39 inch square bars, couples on surface. Portevin and Séférian[1]

(as shown by maximum hardness) can be judged from the calculated values. Conversely the welding speed required for reducing stress and therefore deformation can also be judged from the above relationships, since it is noted that the isothermal lines give the ratio of local to maximum temperature, and also indicate the width of the heat-disturbed area.

The experimental investigations carried out by the authors are particularly valuable in that exact comparisons between the thermal effects of gas- and arc-welding of steel, copper and aluminum are now available. The work involved in preparing the sample plates, the thermocouples, connecting and operating the instruments for recording and observing the temperature-time relationships was a tremendous undertaking, which has made possible the excellent data given in curves of which Figs. 3, 4, 5, 6, 7, 8 and 9 are reproduced here.

It appears that it was possible to make four to six simultaneous readings of temperatures in the heat-affected area with 0.20 mm. diameter Pt-PtRh thermocouples. The curves were registered on photographic paper on a drum driven by a clock-work motor. Thus, the maximum temperature attained along a line perpendicular to the weld bead could be obtained directly from the record. To the reviewer it seems unfortunate that some of these complete temperature-time curves for various distances from the weld and for various welding methods were not reproduced in their publication. It is also noted that with the exception of the gas welds the highest temperature measured 10 mm. from the weld line was 650 to 750° C. (see Figs. 6 and 9) while for the gas welds the temperature maxima 10 mm. from the weld line were about 1000 and 1350° C. (see Figs. 3 and 9). In the region which is considered most critical in the weld, the area of maximum hardness, the curves report extrapolated rather than actual values.

The curves of Fig. 9 are of greatest value according to the authors, in that with it the microstructure developed in welded steels, as a function of distance from the weld and maximum temperature attained, varying with the welding method, may be predicted. The maximum quenching rate is seen to vary directly with the maximum temperature attained and the more drastic nature of the quench for arc than for gas welding is shown.

The effect of the welding process, arc or torch, on the preheating ahead of the heat source and the cooling rate of the thermally disturbed metal is clearly brought out by a study of the three-dimensional figures in Figs. 7 and 8.

Theisinger, W. G. The Heat Effect in Welding. *Iron Age*, **138,** Oct. 1936, pp. 81-82, 86, 88, 90, 92; Dec. 17, pp. 30–45. Vol. 139, Feb. 25, 1937, pp. 28–31, 115; Mar. 4, pp. 48–53.

It has been the privilege of the reviewer to have on hand as an introduction to his own welding research at the Naval Research Laboratory, the complete report by

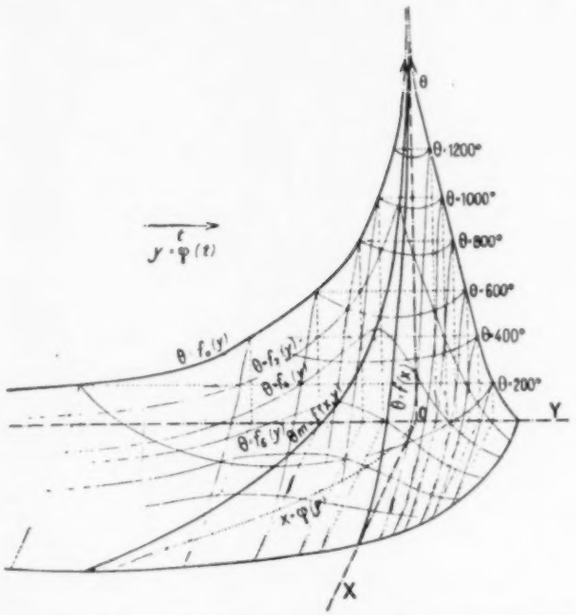

Fig. 7—Temperature Distribution in Oxyacetylene Welded Mild Steel Plates as Torch Is Passing Point 0

OY is the direction of the weld
Arrow shows direction of welding
θ = temperature, ° C. Portevin and Séférian[1]

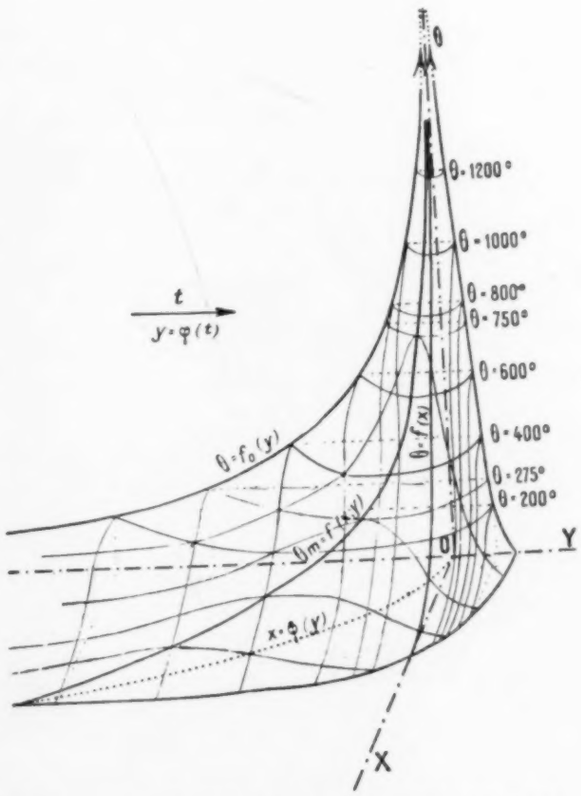

Fig. 8—Temperature Distribution in Covered Electrode Welded Mild Steel Plates as Arc Is Passing Point 0

OY is the direction of the weld
Arrow shows direction of welding
θ = temperature, ° C. Portevin and Séférian[1]

the above author, issued by the Watertown Arsenal. This tremendous undertaking, covering a large number of compositions and conditions of welding has initiated

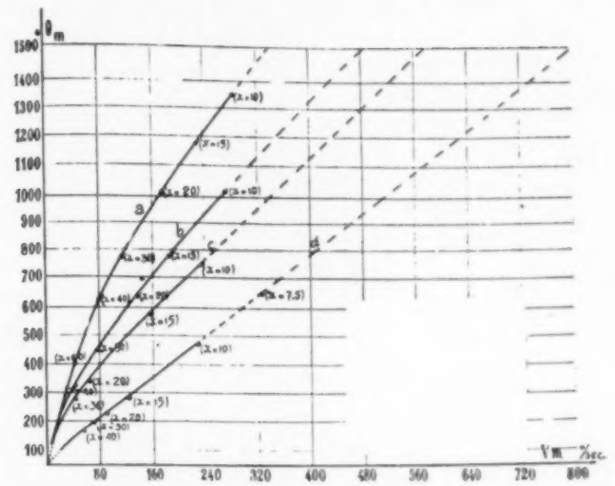

Fig. 9—Maximum Cooling Rates at All Points in the Vicinity of Oxyacetylene and Covered Electrode Welds in Mild Steel

(*a*) oxyacetylene weld, 0.39 inch plates
(*b*) oxyacetylene weld, 0.39 inch sq. bars
(*c*) covered electrode weld, 0.39 inch plates
(*d*) covered electrode weld, 0.39 inch sq. bars
θ_m = maximum temperature attained at any point, ° C.
Vm = maximum cooling velocity, ° C. per second
x = distance in millimeters perpendicular to the weld. Portevin and Séférian[1]

standards of procedure for welding research which may be followed with profit by all those engaged in such investigations. Dr. Theisinger's same title is used for the present review by the writer, which, it is hoped, will be regarded as a token of his personal esteem for the author's work.

The author suggests the use of a simple unit of Joules/inch of weld in order to integrate into one unit the effects of speed of welding and current on the hardening effected in the heat-disturbed area. It is shown that changing the speed or current has a consequent effect in changing the thermal energy supplied to the parent metal through the weld metal, and the simple relation of hardening increasing with decreasing energy input is cited.

The author investigated the hardening of various steels at various speeds of welding by laying a weld bead on a flat plate, sectioning the plate, polishing, etching and indenting with Vickers hardness tester at a load of 10 kilograms. The indents were spaced at intervals of 0.02 inch in the transition zone and 0.05 inch into the parent metal. The results of examination of the excellent series of continuous micrographs are summarized below, with a summary of the hardness tests following next in order. It will be noted that Table 1 indicates ferrite, pearlite, sorbite, troostite and martensite by the capitalized first letter; the notes are Dr. Theisinger's own helpful comments on the structures. In the Tables 2 to 5 giving hardness values the letters W, Z, P, designate maximum hardness of the weld deposit, maximum hardness of the weld transition zone, parent metal hardness, respectively. It is seen that hardening is related by this investigation to composition, welding speed, mass, thermal energy input, thermal conductivity (tests on backing material) and methods of welding (automatic arc, manual arc, gas welding).

The hardening of the metal in the transition zone was regarded as undesirable when the increase in hardness over the original hardness was large. A tentative figure of a maximum increase of 100 Vickers Brinell units was set up, and the maximum allowable speed of welding was defined as that for which the hardness increase was just below this figure.

Table 1—Photomicrographs

Bare Electrode 375 Amps.—23 Volts Automatic Welding

Speed of Welding, In./Min.	4 Inch	6 Inch	8 Inch	12 Inch	18 Inch
S.A.E.1015–0.17 C	Large grains due to long overheating F and P	P structure indication of a slightly faster rate of cooling	P structure cooled a little faster	P structure. Finer grain size	Sorbite. Even this fast rate of cooling produces no heterogeneity of structure
S.A.E. 1025–0.25 C	Large grains, slow cooling F and P	Smaller grain size F and P	Faster rate of cooling. Large grains confined to fusion zone	Sorbito—P in fusion zone	Sorbite
S.A.E. 1035–0.33 C	Increased P	P structure smaller grain size	P	Sorbito—P	Fine grained S
S.A.E. 1045–0.44 C	Coarse grained P with some S	S smaller grain size	S Troostite	Mostly T	T with Martensite patches
S.A.E. 1050–0.53 C	S coarse grained with traces of P	S coarse grained	S-T fine grained weld	T effect of rapid quench	T with large patches of M
S.A.E. 4130–0.33 C Mn 0.5–0.8, Cr 0.5–0.8, Mo 0.15–0.25	S with F in grain boundaries and crystallographic planes	S smaller grains	S fine grained	T	M
S.A.E. 4150–0.52 C Mn 0.50–0.8, Cr 0.8–1.0, Mo 0.15–0.25	S-T large grains	S-T	T-M	M	M extremely fine
Mo steel 0.25 C Mn 0.85, Mo 0.24, Si 0.18		S fine grained characteristic of Mo steels. F in grain boundaries and crystallographic planes	S with F in crystallographic planes	S and a little T	
Ni steel 0.3 C Mn 0.6, P 0.024, S 0.025, Ni 3.45		S	S	M	
Ni steel 0.2 C Mn 0.36, P 0.026, S 0.023, Ni 3.40		S-P	S-P	S	
Ni steel 0.17 C Mn 0.60, P 0.012, S 0.020, Ni 2.15		P finer grain than plain C steel of same C content	P	S	

It would be of considerable benefit in furthering the progress of the welding profession to have the author's complete investigation published in the original form of the Watertown Arsenal report, rather than in the scattered form as given in the series of articles in *Iron Age*.

Warner, W. L. Arc Welding of Structural Alloy Steels. AMERICAN WELDING SOCIETY JOURNAL, **15,** Supplement pages 21–32, Oct. 1936. Trans. A. S. M. E. **58** (7), 515–528, Oct. 1936.

In introducing the subject of investigation the author surveys the factors which affect the magnitude of the heat effect and describes tests which have been standardized for Watertown Arsenal procedure in evaluating the effect of steel composition and welding conditions. The procedure is as follows:

1. Plate dimensions are 9 in. x 3 in. x $^1/_2$ in. thick on which a single weld bead is laid approximately in the center of the 3-inch width.
2. A $^1/_2$-inch section is cut transverse to the bead about 1 inch from the start of the weld.
3. A progressive hardness survey is made on the surfaced cross section.

The reasons for standardizing the procedure in this way rather than on an actual weld are that in the weld the heating and reheating complicate the study and do not represent the maximum heat effect suffered by the first bead laid down. Also the area of crack formation during welding is considered to be in the heat-affected area of the first bead and the single bead tests on the $^1/_2$-inch plate are considered to indicate the most critical condition. The evaluation of cracking tendency as a function of composition or welding conditions is possible with the above procedure.

The author shows the effect on Vickers Brinell hardness of increasing carbon in the parent metal and increasing speed of welding for both the heat-affected zone maximum hardness and the weld metal. The curves given are from Dr. W. G. Theisinger's studies on the subject.

A series of 13 steels in which the low alloy contents represent the nickel steels, Cu, Ni, Mo, Mn, Si, Cr, P combinations, of commercial steels designed for good weldability. The mass of data assembled for these steels by means of the standard heat effect tests on $^1/_2$-inch plate, other tests of mechanical properties including tensile impact are given in many tables, curves and graphs too numerous to reproduce here.

The author considers hardenability a gage for weldability for steels in the equal strength class in the parent plate. Carbon is judged as the main factor in lower weldability on the above basis. For the structural nickel steels the nickel content is contributory to the higher hardenability, but carbon is more potent in increasing the hardness.

A rather odd and unexplained heat effect is shown in curves reproduced from International Nickel Company data for two speeds of welding.

Here the trend is toward equal and maximum hardening with increase in nickel for both speeds of welding. The maximum is reached at 0.45% carbon with 3$^1/_2$% nickel. It would appear to the reviewer that the disturbed area where the maximum was measured is completely martensitic under the test conditions.

Fry, A. Problems in the Arc Welding of Steel. *Elektroschweissung*. **4,** No. 11, 201–209, Nov. 1933.

Fry remarks that it is customary to regard the coarse

Table 2—Hardness Data for Various Welding Speeds

Bare Arc 375 Amps.—23 Volts

Automatic S.A.E. 1015	Welds 4 Inches	6 Inches	8 Inches	12 Inches	18 In. per Min.
Weld	126	126	160	174	215
Zone	151	158	160	164	185
Plate	120	121	121	120	120
S.A.E. 1025					
W	124	137	151	174	221
Z	148	148	151	168	206
P	130	130	134	134	131
S.A.E. 1035					
W	131	145	160	187	240
Z	162	168	177	212	250
P	139	139	140	140	140
S.A.E. 1045					
W	148	162	170	190	266
Z	218	218	240	266	333
P	177	177	177	177	177
S.A.E. 1050					
W	153	160	183	224	369
Z	218	233	278	322	427
P	190	195	193	195	195
S.A.E. 4130					
W	177	200	221	243	327
Z	237	230	240	270	464
P	203	203	209	209	209
S.A.E. 4150					
W	206	212	246	302	455
Z	302	302	333	548	642
P	278	287	287	287	292
0.3 C—3.5 Ni					
W		172	193	246	
Z		258	278	333	
P		212	218	218	
0.2 C—3.5 Ni					
W		166	179	240	
Z		215	218	282	
P		185	185	187	
0.17 C—2.15 Ni					
W		160	166	212	
Z		185	187	240	
P		166	164	166	

Table 3—Hardness Data for Various Welding Speeds

Automatic Welds

	4 Inches	6 Inches	8 Inches	12 Inches	18 In. per min.
0.25 C—0.24 Mo					
W		155	179	215	
Z		212	224	258	
P		181	183	183	
0.15 C—0.48 Mo					
W		148	185	218	
Z		206	221	237	
P		177	179	179	
After Stress Relieving at 1150° F.					
S.A.E. 1035					
W	128	139	150	183	215
Z	166	170	181	193	200
P	140	141	142	140	140
S.A.E. 4150					
W	270	270	274	345	390
Z	278	278	312	351	376
P	254	270	274	254	262
0.9 C (1/8 in. x 1 in.)					
W			162	166	
Z			302	322	
P			230	230	

grain, overheated structure of steel as a sign of a considerable injury to the material. Data are then presented for notched-bar impact value of Izett IV. Steel samples before and after arc welding. The information as to material analysis and welding conditions is lacking.

Table 4—Effect of Thickness on Hardness

Automatic Welds on 1/8-2 In. x 2 In. x 9 In.—0.43 C Steel A 2 In. x 2 In. Bar Was Forged Down to Various Thicknesses Amps.—375, Volts 23, Speed—8 In./Min.

	1/2 Inch	3/4 Inch	1 Inch	1 1/2 Inches	2 Inches Thickness
W	170	185	193	197	209
Z	254	274	302	307	312
P	179	179	177	174	174

Effect of Backing Material

S.A.E. 1050

	Steel	Copper	Asbestos
Weld	183	181	170
Z	278	287	258
P	193	193	193

Table 5—Hardness Data for Various Welds

Hand Welds

S.A.E. 1045	Covered Electrode	Bare Electrode
W	168	195
Z	258	282
P	177	175
S.A.E. 4130		
W	190	195
Z	258	282
P	209	212
0.30 C—3.5 Ni		
W	193	203
Z	257	383
P	209	209

Nickel Steels (covered electrodes)

	0.17 C—2.15 Ni	0.20 C—3.5 Ni	0.30 C—3.5 Ni
W	193	230	230
Z	200	327	363
P	155	181	195

Gas Welding

	0.17 C	0.44 C
W	148	148
Z	160	227
P	122	181

The results of notched impact tests for a series of furnace treated samples (1 hour at temperature) of the same type of steel are given, but no information is available as to the size of the samples treated. The micrographs of the welded material and the furnace treated material, presumably at the same magnification indicate that the grain size of the treated samples is considerably larger than the largest grains in the weld transition zone.

Table 6—Welded Izett IV

Impact (DVMR-test)

Parent Metal	Parallel to Rolling direction	11.0 mkg./cm.²
	Across " "	9.9 "

Transition zone

Parent Metal	Parallel to weld	12.5 "
		10.2 "
next to weld bead	Transverse to weld	10.6 "
		12.0 "

Furnace Treated Izett IV

Heated 1 hour at	Cooled in air	Cooled in furnace
	Impact	
870° C.	16.3 mkg./cm.²	11.5 mkg./cm.²
1000° C.	13.4 "	8.6 "
1200° C.	12.5 "	8.2 "
1300° C.	9.5 "	7.9 "

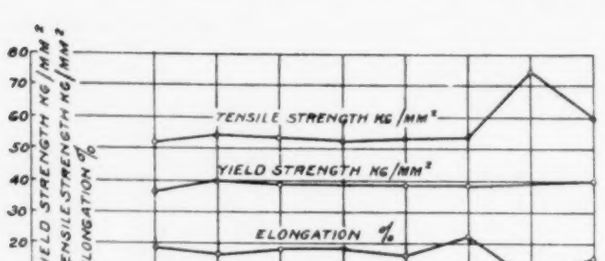

Fig. 10

Treatment
(1) As Received, Plate 6 Mm. Thick
(2) Arc Welded, Vee Butt Weld, Bare Rod, Bead Ground Flat
(3) " " As in (2), Rewelded and Bead Again Ground Flat
(4) " " 12 Mm. Strap Weld, Light Double Bead, Ground Flat
(5) " " 12 " " " Heavy " " " "
(6) Cooled in Still Air from 900° C.
(7) " 10 Sec. in Oil from 900° C.
(8) " 1 Sec. in Water from 900° C.
Bending Radius for Treatment 1, 6 & 8 > 180°
" " " " 7 = 87°

The data are intended to show that in the welded sample there is no loss of toughness due to overheating of the parent metal in the transition zone of this steel. The treated samples are considered to show that although excessive grain growth results from overheating, the loss of toughness is not large and since the grain growth is greater than in the transition zone we should expect still smaller loss of toughness in the latter. The conclusions, however, would have been more convincing if hardness data had been included to indicate how closely the synthetic transition zone samples checked the actual weld.

There is then given the results of a series of tests for notch impact of the various zones as below, of the Izett IV steel welded with an austenitic electrode since the author considers the non-aging Izett IV welded in this manner an ideal combination.

Table 7—Izett IV Welded with Austenitic Electrode

	Noth Impact Average of 2 Tests
Parent metal	11.4 mkg./cm.²
Martensitic area of transition zone	15.5 "
Austenitic weld	12.9 "

A discussion of quench sensitivity of welding grades of steel is given in which it is shown that a constructional steel, St52, with tensile strength of 74,000 lb./in.² which had too high a quench sensitivity for arc-welding was modified in analysis (details not given) by reducing the carbon content and increasing the alloy. A series of tests are reported of mechanical properties of two thicknesses, 6 mm. and 14 mm. of this modified St52 steel which are given below. The low quench sensitivity of the material is considered to be indicated by the flatness of the curves for the variety of treatments given the samples as shown below in Figs. 10 and 11.

These data are of particular importance in showing that treatments 6, 7, 8 might be used to indicate the limits of injury to the parent metal transition zone for different conditions of arc-welding. It is the reviewer's belief that the highest quenching rate of the transition zone for manual welding will be between the rates given by treatments 7 and 8. It will also be observed that treatments 7 and 8 are somewhat misleading since the mass of the samples quenched for 10 and 1 seconds, respectively, in oil and water is not stated. The cooling rate for treatments 7 and 8 especially would be profoundly affected by the sample mass, and if the mass were large, the short duration of the quench would entail a considerable microstructure gradient of the cross section.

Data is given for a simplified test of quench sensitivity based on the bending radius given below. The original St52 (Material B) and the grade modified to lower carbon, higher alloy content (Material A) are shown to give approximately the same mechanical properties originally, but show a wide difference in bending after quenching.

Table 8

	Material A	Material B
Yield point kg./mm.²	40.7	40.0
Tensile strength kg./mm.²	58.7	56.9
Elongation %	29.5	28.1
Bending radius for first crack after quenching in water from 1020° C.		
6 mm. plate	180°	170°
12 " "	140°	100°
18 " "	110°	50°

It is not stated why this particular quench was chosen nor how the hardness and microstructure compare with the transition zone of the weld. It is, therefore, difficult to translate the results obtained directly into what to expect in welding the materials.

Musatti, I. and Reggiori, A. Metallographic and Mechanical Characteristics of Electric Arc Welds. *Metallurgia Italiana*, **26,** 303–321, May 1934.

The authors in this article discuss tests on weld metal for change in composition of electrode in welding, physical properties, etc., but their work specifically on the heat effect on the transition zone of the parent metal will only be reviewed. The transition zone is regarded as three areas in which (1) the metal has been overheated, (2) normalized, (3) subcritically annealed (slowly al-

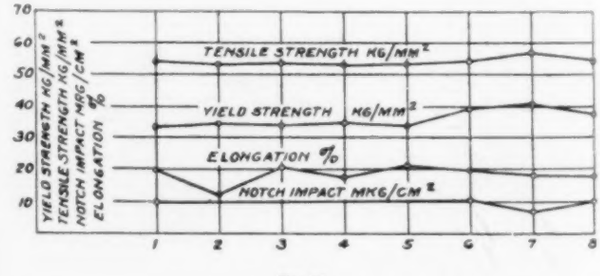

Fig. 11

Treatment
(1) As Received, Plate 14 Mm. Thick
(2) Arc Welded, Vee Butt Weld, Bare Rod, Bead Ground Flat
(3) " " As in (2), Rewelded and Bead Again Ground Flat
(4) " " 12 Mm. Strap Weld, Light Double Bead Ground Flat
(5) " " 12 Mm. " " Heavy " " " "
(6) Cooled in Still Air from 900° C.
(7) " 10 Sec. in Oil from 900° C.
(8) " 1 Sec. in Water from 900° C.
Bending Radius for Treatment 1, 6, 7 & 8 > 180°

tered). A number of progressive Vickers hardness curves are given from weld to undisturbed parent metal superimposed on the micrograph and in many cases the hardness is a maximum not immediately adjacent to the fusion line, but at a slight distance from the line. This the authors ascribe to diffusion into the weld metal. Particular attention is paid to the large grain zone next to the fusion line where the base metal containing 0.23% C, 0.82% Mn, 0.28% Si is said to be a Widmanstaetten type of structure. Quench tests were made on temperature gradient quenched samples in which one end of a bar 150 mm. long was heated to incipient fusion in a Tammann furnace while the other end was artificially cooled. Three such bars were cooled in water, air and in the furnace and the microstructure and hardness determined. The graphs of hardness show a gradient, maximum hardness at the heated end and decreasing toward the cooled end of the bar. The microstructures are given for a point near the hot end and in the middle of the bar, they show a grain size gradient which follows the hardness gradient. The structures are shown to be martensite for the water quenched bar, Widmanstaetten for the air-cooled bar, pearlite and ferrite for the furnace-cooled bar. The Widmanstaetten structure is, therefore, regarded by the authors as the result of a mild quench intermediate between martensite and troostite. The structure is thus a pseudo or "nascent" martensite. The hardness corresponding to the microstructure shown is indicated by a 5 kg. Vickers indent, the hardness being as follows:

	Water Quench	Air Cool	Furnace Cool
End of bar	509	212	160
Middle of bar	317	177	156

The writer notes that the grain size of the hot end of the rod is considerably larger than that found in their welds. The structure of the air-cooled bar in which the Widmanstaetten pattern was produced is a true Widmanstaetten structure, whereas the weld transition zone is a quite different one in which the ferrite is more or less confined to the grain boundaries, and the carbide precipitated in more uniform and disperse form. The hardness of the air-cooled bars is also considerably less than the maximum hardness of the transition zone.

Another series of tests is reported in which fatigue, tensile, ductility and impact values were obtained for specimens of the base metal which were overheated and cooled to give the Widmanstaetten structure. The micros for these samples appear to be a better reproduction of the welded sample than the temperature gradient-quench bars. The details of the heat treatment required to produce the structure are not given. The fatigue limit of 33.0 kg./mm.^{2} and ultimate strength of 70.1 kg./mm.^{2} are higher values than obtained for base metal, welded sample or weld metal, as shown in Table 9.

From the tests it is concluded that the overheated area of large grains with the Widmanstaetten structure found in the transition zone does not have inferior properties in fatigue, tension, impact than found for the weld or parent metal and is therefore not a critical area in a welded section of the composition investigated. This conclusion is borne out by the values reported, and it is somewhat surprising to the writer that the impact values should be so much higher for the overheated metal than for the base metal.

In view of the low impact values found for the Widmanstaetten structure by Portevin and Bernard (see reference No. 14) and by Kinzel for synthetic overheated transition zone metal for a gas weld (see reference No. 7), it would appear from the present authors' work that arc-welding of a mild steel of the composition investigated is less injurious to impact strength of the transition zone than the same material when welded with the oxy-acetylene torch.

Table 9

Material	Fatigue Limit Kg./Mm.^{2}	Breaking Strength Kg./Mm.^{2}	Fatigue Ratio
Base metal	28.9	60.1	0.481
Welded joint	19.4	57.4	0.338
Weld deposit	20.4	58.9	0.346
Overheated base metal	33.0	70.1	0.472

Other tests made on the synthetic transition zone material show:

Ultimate Strength Kg./Mm.^{2}	Yield Point Kg./Mm.^{2}	Elongation	Reduction of Area	Impact Kg./Mm.^{2}	Impact (Ft.-Lb.)
72.7	45.8	20%	60.5%	8.6	(62.3)
67.6	39.5	22%	61.7%	8.4	(60.9)
				8.5	(61.6)

Average impact for undisturbed parent metal 7.2 kg./mm.^{2} (55.2 ft.-lb.)

Meunier, F. and Rosenthal, D. Electric Arc Welding of Steels Other than Mild Steels. *Rev. Met.*, **33** (10), 588–593, Oct. 1936.

The factors which affect the weldability of steels are tabulated as:

1. mechanical—internal stress and deformation.
2. physico-chemical—changes of phase or structure.
3. chemical—changes in composition.

The authors discuss the physical-chemical changes which occur in the transition zone and consider that for steels with more than 0.40% carbon the cooling rate in the areas which attain temperatures of 800 to 500° C. the thermal cycle of the welding operation produce in them a quenching rate exceeding the critical rate with the consequence of fissures appearing in the transition zone. This problem was investigated by increasing the current with a constant speed of welding and by keeping the current constant with increasing speed of welding. The microstructures obtained in the transition zone of a 0.44% carbon steel for these conditions are shown and indicate decreasing amounts of martensite for increasing current (100, 300, 400 amps.) at constant welding speed (18 cm./mm.) With the lowest current of 100 amps., the microstructure indicates fissuring took place around grain boundaries, the structure being completely martensitic. With 180 amps. at 9 cm./min. welding speed the grains are large but sorbite is formed while at 4.5 cm./min. the grains are still larger with the formation of pearlite. Curves are drawn for the index of heterogeneity (of properties, which is equivalent to per cent hardenability of the base metal) *vs.* current and welding speed showing an increase of heterogeneity with decreasing current or increasing speed of welding. A simpler statement would probably be that the heterogeneity increases with decrease in energy input per unit of weld length.

A series of carbon steels ranging from Armco iron with 0.02% carbon to steels of 0.10, 0.26, 0.33 and 0.44% carbon and another series of low alloy steels were welded in one pass and in four passes. The values obtained in tensile, bend, impact and hardness tests are tabulated by the authors (this tabulation appears with titles in English in AMERICAN WELDING SOCIETY JOURNAL, **15,** 10, appendix 1, Nov. 1936. The heterogeneity of the properties is again considered and appears to increase with increasing carbon and decreasing number of passes.

The increase of hardness and decrease in bending angle

with carbon increase is much more marked with the single pass than with the four pass weld. This is also true of the bend ductility and impact (Mesnager notch) values, the latter being more sensitive to carbon increase than any other properties determined. The tests performed lead the authors to consider carbon steels with carbon contents of 0 to 0.25% as perfectly weldable, 0.25 to 0.40% carbon semi-weldable and beyond 0.40% carbon as unweldable.

For the carbon steels a series of micrographs is shown which indicates the variation in structure of various areas of the transition zone due to increasing carbon.

Kinzel, A. B. Metallurgical Effects Produced in Steel by Fusion Welding. A. I. M. M. E. Tech. Publication 597, Feb. 1935 meeting.

This paper is readily available as an English condensation of the more ambitious paper prepared in French by the author (Study of Testing Methods for Oxyacetylene Welds on Steel Plates. A. B. Kenzel. Diss. *École Superieure de la Metallurgie*, 1933, 89 pages). It is of interest to point out the temperature-time curve as one of the first such curves for a gas weld where the complete curve (at high temperature) was presented for an area adjacent to the weld. The maximum temperature measured for this particular curve was 1125° C., and the slow rate of cooling can be seen from the curve by noting that the drop from 1125 to 100° C. takes approximately 65 minutes. Some comparison of the relative extent of the heat-affected zone of the gas and the heavy-coated electrode arc weld can be made in comparison of the continuous micrograph of the gas weld for $1^1/_8$ inches of the transition zone where the temperature attained was 700° C. while the arc weld, shown at higher magnification, indicates the 700° C. temperature was attained 0.10 inch from the fusion line. In the original French publication the continuous micrograph of the gas weld is at a magnification of 50× and is approximately 5 feet in length, which permits following in detail and at a glance the metallurgical changes occurring across the transition zone.

The most interesting part of the paper is the table, which surveys the mechanical properties of the various regions of the heat-affected area as determined on miniature cold bend samples and by hardness tests as compared with samples of the material heat-treated at high temperature to duplicate hardness and microstructure of corresponding areas of the weld transition zone of a gas weld. It is significant to note that for ductility and impact resistance there are two minima, one in the zone immediately adjacent to the fusion line where the temperature attained was 1400° C. and the other at 800° C., 1.04 inches from the scarf. A maximum of strength occurs at the highest temperature with highest hardness and a minimum at 700° C. where the hardness and strength dip below the parent metal values due to the subcritical anneal. However, the ductility for this latter zone is shown to be a maximum, and therefore not critical for service.

The lowest Izod impact value of 8 ft.-lbs. for the temperature of 1400° C. which is 20% of the original impact of the plate appears to the writer to have been made exaggeratedly low by the heat treatment since inspection of the micrographs indicates the synthetically treated materials with a structure in which the ferrite exists more as a grain boundary network than in the corresponding zone of the continuous micrograph. In the latter case the dispersion of ferrite and pearlite is more uniform, and almost eliminates the grain boundaries and can, therefore, be expected to show better impact and ductility values than the furnace-treated material of equal hardness.

The inclusion of some values of Izod impact on the weld transition zone itself, as usually made by putting the notch in various areas of the zone next to the weld, would have constituted a gage as to the reliability of the latter test since a comparison would have been possible with the other values obtained by the author. Furthermore, since the samples were air cooled, it would have been comparatively easy (compared with a liquid quench) to check the temperature-time cycle of the synthetically treated samples in order to assure that the thermal cycle had been duplicated.

Such a series of tests as the author reports, would appear to be a good method of evaluating the relative suitability of gas- or arc-welding for certain steels if the corresponding series of tests for the arc welds on the steels were available for direct comparisons.

A chronological bibliography from 1857 to 1932 is appended in the author's French publication, with approximately 225 names.

Kleiner, H., and Bossert, K. The Heat Effect During Welding on the Tensile Properties of the Bond and Parent Metal. *Autog. Metallb.*, **27,** No. 9, 131–139, May 1, 1934. The tests were made on a welded plate consisting of two plates 300 mm. x 1050 mm. and 16 mm. thick joined by left-hand and right-hand oxyacetylene Vee butt weld along the 1050 mm. length. The tests were initiated because it was found in tensile tests that rupture always occurred in the heat-affected zone parallel to the weld bead. Tests of tensile strength, hardness, impact, repeated bend and microstructure were made on boiler steel (composition not given). Samples were cut from the plate for the various tests, parallel with and across the rolling direction and the weld bead. A number of maximum temperature readings were made at seven positions along four lines at right angles to the bead, the lines being disposed at beginning and end of the weld, and at $^1/_3$ and $^2/_3$ of plate length from either end. The maximum temperature *vs.* distance from bead curves given in Fig. 12 show a smaller heat input (steep gradient) for the right-handed weld than for the left-handed torch weld. At 20 mm. distance from the weld the left-hand method shows a temperature maximum about 300° C. higher. Macros of the two plates tested show laminations due to dirt and bubbles which are considered an element of danger in promoting rapid failure by repeated bending.

The investigation of microstructure is briefly reviewed with the conclusions that the weld is a coarse grain cast structure and the transition zone in the areas attaining a temperature of 1200° C. or over shows a Widmanstaetten structure with no evidence of a preferred orientation. The structure is such as to be weak in impact. The banded structure in the parent metal is caused to disappear gradually into the transition zone area to a uniform dispersion of pearlite and ferrite in large grains. The heat effect is shown to affect the properties of the metal beyond the zone shown to be affected in the macrostructure where temperatures below 1200° C. were attained.

In the tensile tests no difference greater than 1 kg./mm.^{2} could be found in either, tensile, yield strength nor any difference in per cent elongation for the various zones (see Fig. 12). The notch impact values show the greatest sensitivity in picking out different regions of the heat-disturbed zone, and the fatigue test values also show the effect of the different microstructure in the heat-disturbed area.

For the hardness measurements a "Testor" hardness measuring device (the particular tester used is not described) using a load of 62.5 kg. and the values so obtained were converted by means of a conversion curve

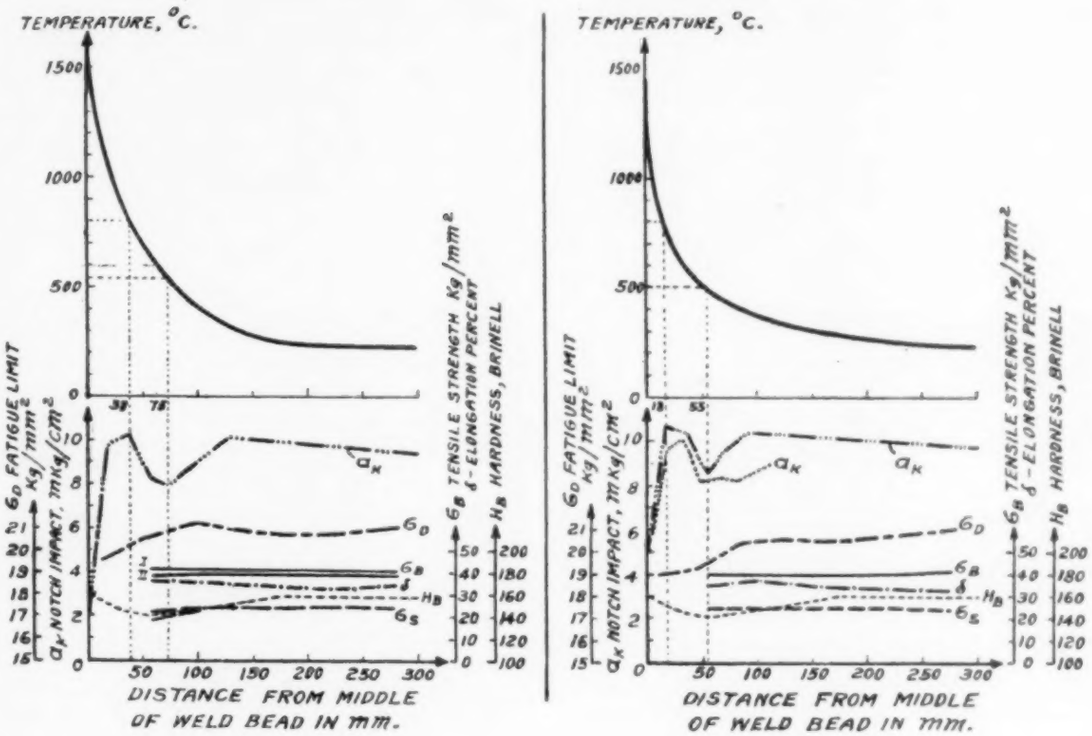

Fig. 12

into Brinell units. The author states that between the left- and right-handed welding methods, no appreciable difference could be found in either extent of hardening, nor in the hardness differential between various zones. The dashed-line curves of Fig. 12 bear this out, and furthermore, show a decrease in hardness in the region immediately adjacent to the weld bead. The author ascribes this to the large grain size developed in this region, and cites the generally accepted fact that fine grained steel is harder than the same steel with large grains.

For the impact values given in Fig. 12, from 8 to 12 values were averaged to give a point on the curve, but it will be noted that the curves are for step-wise rather than progressive values from bead to undisturbed parent metal. The value at the origin is for the weld metal, the next value is 1100° C. maximum temperature zone (left); 800° C. max. temperature zone (right-hand weld). The improvement in impact value in the regions where a temperature of about 790° C. was attained is ascribed to the disappearance of the banded structure of the parent metal. The dangerous range of temperature for loss of toughness is, therefore, considered to lie between 760 and 380° C. where the impact values dip below those of the parent metal. The authors consider three possible causes for low impact values; (1) overheating, (2) deformation in blue-heat region (200 to 400° C.), (3) critical annealing 650 to 850° C.) after critical deformation (8 to 10%) of which none of the three are believed to apply to the sharp drop in impact value at 500 to 550° C.

The fatigue tests are considered as a better indication of the effects of the thermal disturbance than the impact tests in that the fatigue limit is dependent upon the grain size and internal stress of the grains as well as on the mechanical effect on the material due to the stresses set up in heating and cooling. The fatigue resistance drops below the parent metal from 400° C. upward, and is reduced about 10% below the original value in the region close to the bead.

Lebrun, M. Maurice. Distribution of Temperatures and Stresses in Plates Welded by Different Processes. *Bull. de la Société des Ingenieurs Soudeurs*, **5**, No. 30, 1326–1331, May–July 1934.

Two square plates of steel of unstated analysis, 250 x 250 mm. and 15 mm. (0.591 inch) thick were butt welded as follows:

1. Oxyacetylene weld—2000 L. acetylene/hr. the 250 mm. length of weld being made in a single pass in 24 minutes. A series of 4 mm. diameter drilled holes were made on a line perpendicular to the weld in the middle of one of the plates starting 10 mm. from the scarf, and at intervals of 10 mm. up to 200 mm. distant from the scarf. Thermocouples were put into the drilled holes and maximum temperatures were measured. The results are given in a table which shows 1120° C. to be maximum temperature measured 10 mm. from the scarf. The heat effect is stated to be limited to the zone within 40 mm. from the scarf where the temperature reached 490° C. Hardness tests in Brinell units are reported, which indicate practically same level of hardness for zone 90 mm. from scarf and a slight drop after 100 mm. from the scarf.

The stress value was indicated by measuring a 25 mm. drilled hole in the other plate butt welded to the plate with the drilled holes for measurement of temperature.

The hole was placed in the middle of the plate at a distance of 50 mm. from the scarf. On completion of the weld the hole diameter was 25.4 mm. perpendicular to the weld and 24.7 mm. parallel to the weld indicating tension and compression, respectively.

2. Arc welds were for butt-welded plates of same dimensions as above. Several passes were made using 4 mm. diameter electrode, coated type with 130 amperes. The speed of welding is not given, and the elapsed time before starting the next pass is not stated. It is noted that the second pass had a cross section double that of the first pass.

The same tests of temperature and stress were made as for the torch weld. The maximum temperature measured 10 mm. from the scarf is shown to be 250° C. and 285° C. for first and second pass, respectively. The deformation of the 25-mm. hole due to stress was considered negligible.

3. Atomic hydrogen butt weld on plates as above was made in two passes. The arc was fed with three-phase current of 80 amps. at 220 volts. It is stated that the plate was not cooled before beginning the second pass. The total time of welding was 6 minutes. Measurement of the 0.25-mm. hole showed 25.26 and 25.28 mm. after the first pass and 25.24 and 25.4 mm. after the second pass for the diameter perpendicular and parallel to the weld, respectively. A hardness survey showed little increase in hardness next to the weld, which is considered to be due to decarburization in the neighborhood of the bead.

It will be noted that the above work shows actual temperature measurements in the transition zone, which are of interest in defining the important metallurgical changes in the parent metal, were not accomplished for other than the oxyacetylene weld. In the latter case only part of the transition zone was covered since the maximum temperature measured was only 1120° C. and the zone where maximum hardness occurs was therefore not reached.

A plot of temperature maxima is given showing the distribution of maximum temperature attained from 10 mm. through 200 mm. from the weld line for (1) first and second pass of electric arc weld, (2) first and second pass of atomic hydrogen weld, (3) oxyacetylene weld. The maxima, respectively, are: (1) 285° C., 305° C., (2) 400° C., 525° C., (3) 1120° C.

Roberts, A. T. A Study of the Thermally Disturbed Areas in Carbon Steels. *The Welder*, **8,** 647–651, Aug. 1935, I. & S. Inst., **II,** Welding Symposium 1935, pp. 531–537.

This author gives an idealized maximum temperature *vs.* time curve for an arc weld, and notes that the form of the curve changes with change in mass, specific heat and initial temperature of the parent metal. A more favorable condition for dynamically stressed welds with respect to distribution of stress over a wider heat affected zone, is indicated for electrodes which impart a greater quantity of heat per unit volume of weld metal.

The experimental work was done on arc, butt-welded 4 inch x 2 inch x 0.30-inch plates with composition.

The welds were made with 8 gage heavily coated rods at an unspecified speed, and were surveyed with 2-mm. steel ball for hardness. The results are given in Table 10 as taken from the author's results. A review of the microstructure of the transition zones of these steels leads the author to postulate the formation of sorbite in the areas immediately adjacent to the fusion line, due to the apparent, but not actual, increase in amount of carbide; and no evidence of martensite or troostite for these compositions under the welding conditions employed.

Table 10

	C	Mn	Si	S	P	Max. Hardness As Welded	Max. Hardness Stress Relieved
A (hot forged)	0.523	0.71	0.22	0.027	0.028	277	255
B (rolled plate)	0.175	0.485	0.055	0.044	0.048	163	140
C (" ")	0.054	0.325	0.010	0.068	0.080	153	128

Pinczon, M. J. Electric Welding in Naval Construction. Problems of Stress. *Science et Industrie*, 17, Section 234–316, July 1933.

With the aid of purely mathematical considerations, the author derived a curve for maximum temperature *vs.* distance from the weld for a weld on thin sheet in which the isothermal lines are exactly parallel to the weld bead. The curve is practically the same as the one given by A. T. Roberts, as noted above.

Davenport, E. S. and R. H. Aborn. Metallurgical Aspects of the Welding of Steel. AMERICAN WELDING SOCIETY JOURNAL, **15,** 21–31, Oct. 1936.

The fundamental metallurgical considerations of the heat effect in the welding of steel are given in this paper. The region of temperature from the weld metal to undisturbed parent metal is mapped into five zones which are:

1. Above the solidus (liquid metal).
2. Solid-liquid (mushy metal).
3. Between solidus and A_3 (gamma phase).
4. Between A_3 and A_1 (alpha-gamma phases).
5. Below A_1 (alpha phase).

Of these, only zone 4 is called the transition zone, since the alpha-gamma transformation takes place here. Two regions of zone 3 are distinguished as region of grain growth for upper part and region of grain refinement for lower part.

The changes in structure occurring as a result of the heating and cooling are discussed by the authors on the basis of well known metallurgical principles. The factors influencing the structural changes are summarized as:

1. Composition.
2. Rate of heating.
3. Maximum temperature attained, and time interval at this temperature.
4. Rate of cooling.

The series of temperature time curves given in this paper are idealized curves for a single pass butt weld, and are not reproduced from actual temperature measurements made by the authors.

Chevenard, Pierre A. and Albert M. Portevin. Micro-Mechanical Study of Welds. I. & S. Inst. Welding Symposium. Vol. II, 1935, pages 333–352. P. Chevenard. *Metaux*, **10,** 37–49, Feb. 1935. *Bull. d. l. Soc. des Ingenieurs Sodures*, **6,** 1760–1772, May–July 1935. *Welder*, **7,** 614–623, 640, July 1935. The micre-machine is discussed and described in a number of publications along with the results obtained by its use. The present reference is in the English language, and a translation of the original French publication on the machine itself is available in the AMERICAN WELDING SOCIETY JOURNAL, **15,** 11–13, Mar. 1936. Its use is proposed for obtaining progressive values across the weld and transition zone of properties not indicated by the resistance to deformation as given by the usual hardness surveys. The micro shear test is considered as best for determining ductility since these determinations can be made at intervals of 1 to 1.5 mm. (0.04 to 0.06 inch) transversely across the welded section.

The large grain structure near the fusion line usually shows a decrease in ductility with increase in ultimate and yield strength in shear and tension. There are

shown a number of progressive curves for these values for different types of steels welded with gas torch and electric arc, but it is unfortunate that hardness curves are not given with these values to permit a comparison to be made. The relationship of increase in strength with decrease in ductility is not entirely consistent for the series of steels welded and in some cases the ductility remains the same or increases slightly while the neighboring specimen shows a higher strength. These are factors which it is important to tabulate for the different steels or welding methods. However, as a survey of the heat effect, the expense in time and preparation of the many test samples required even with the greater amount of information obtained can hardly counterbalance the rapid method of surveying the cross section for hardness with the Vickers hardness testing method. The latter permits hardness surveys to be made at intervals of 0.02 inch in the hardened transition zone, and is, therefore, especially suited for arc welds where the transition zone is narrow, while the micro-machine seems to be most useful for the gas-torch welds where specimens need not be taken at very short intervals.

A figure of weldability is proposed in which the heterogeneity of the mechanical properties across the weld is evaluated by putting into the equation the values determined by the micro-machine. This is practically equivalent to the statement of maximum hardness and progressive variation in hardness obtained from a hardness survey, although as noted above no evaluation of the ductility of the areas is possible with the hardness tests alone.

Belaiew, N. T. and D. Seferian. A Study of the Widmanstaetten Structure in Welds. *The Welder*, **8,** (No. 33, New series) August 1936, pages 1041–1048, Sept. pages 1079–1082, 1084. This study has been carried out principally as an investigation of the weld deposit structure, but the principles involved in linking metallographic structures with the thermal cycle and metal composition are applicable to the part of the transition zone adjacent to the fusion line.

The metallographic features produced by heating and cooling steels are grouped into four categories of appearance of the grain boundaries, and orientation of the carbide phase. According to the authors, the structure is granular, Widmanstaetten, net work or ferrite margin, depending upon the carbon content of the steel and cooling velocity. Overheating (above A_3) of the steel has the effect of increasing the carbon range in which the Widmanstaetten structure is produced, and in permitting the formation of this structure at lower cooling speeds. Many examples of the Widmanstaetten structure are given in the reproduced micrographs. The stability of the structure as judged by its resistance to refinement by the subsequent welding bead is shown to be high.

The impact resistance of the Widmanstaetten structure is shown to be lower than the best heat treatment for the material in a reference to work done by Portevin and Bernard (no literature source is cited) given in Table 11.

In several curves of progressive values across the weld into the parent metal for hardness, tensile, yield strength and elongation as determined by the Chevenard-Portevin micro-mechanical testing machine, the Widmanstaetten structure lies between the fusion line and the sorbitic areas of maximum hardness for both arc and torch welding.

In another case where martensite was formed at the fusion line, troostite coming next there is shown to be an area of the Widmanstaetten structure between troostite and the undisturbed parent metal. The Widmanstaetten structure from this evidence appears to be associated with a cooling rate slightly lower than that which produces troostite, which is at variance with Musatti and Reggiori's (paper No. 5) conception as being between martensite and troostite.

The authors conclude that oxyacetylene welding favors the formation of the Widmanstaetten structure, which latter may be formed from an initially liquid or overheated metal state.

Anonymous. Review of Acetylene Institute Investigations. The Weldability of Steels. *Genie Civil*, CIV, No. 11, 252–253, (1934). An increase in carbon content of steel lowers the weldability rating but it is noted that below 0.05% carbon the weldability is again low due to oxidation of the steel in welding. With low carbon content the manganese and silicon are increased to prevent burning and 1.0 to 1.2% manganese is favored since this content does not affect weldability. This manganese content also favors formation of small grains near the weld, which in turn has a beneficial effect on mechanical properties, especially impact.

Where the carbon content is below 0.10% there is no beneficial effect in silicon additions, although for carbon contents higher than this, addition of silicon is found to be necessary to give good weldability.

A number of compositions which have good weldability are cited as in Table 12.

The steels for French naval construction having high elastic limit with carbon 0.15–0.20, usually, 1.0% manganese with contents of Ni, Cr, V, Mo, etc., have not been intensively studied, but it is noted that occasionally they have given trouble in welding.

Sutton, H. Welding In Aeroplane Construction. *Aircraft Eng.*, **7,** 178–180, July 1935.

Normalizing of structures for aircraft use is preferred after welding because of coarse grain structure near weld with oxyacetylene welding. English practice leans to-

Table 11

Charpy Impact (Ft.-Lb.)

Composition C	Composition Mn	Widmanstaetten Structure	Hardened 850° C. Water 700° C. Reheat
0.17	0.65	52.8	151.8
0.22	0.71	76.6	150.0
0.32	0.56	43.4	94.0

Table 12

C	Mn	Si	S+P	Cr	Cu	Mo
0.05–0.15	0.30–0.60	Trace (0.08 max.)	0.03 max.			
0.15–0.20	0.80–1.0	0.30–0.40	0.03 "			
0.10–0.15	0.60–0.80	0.20–0.30	0.03 "	0.30–0.40	0.40–0.50	0.10
0.06–0.09	0.20–0.35	Trace		0.10–0.20	0.45–0.55	0.10
0.05–0.07	0.15–0.18	"	0.10 Ni	0.15	0.40–0.60	

ward use of low carbon steels with 1.0 to 1.75% manganese due to their reduced coarsening tendency in the microstructure near the weld as compared with the plain carbon steels.

Chapman, E. C. Welding Low Alloy Steels in the Pressure Vessel Industry. AMERICAN WELDING SOCIETY JOURNAL, **14,** 2–6, Nov. 1935.

Rather than rewrite the adequate language of this author, the following quotations are given: "The practical limit of carbon for welding is determined by the natural quenching effect which the metal receives when deposited in parent metal, having a reasonably low temperature. The fusion zone and affected area are hardened, and with higher carbon it has been found that the ductility is not restored sufficiently by subsequent stress-relieving operations."

Chapman, E. C. Welded Structures. *Jour. Am. Soc. Naval Engrs.*, **48,** No. 4, 476–483, Nov. 1936.

Shop-cracking of welded joints is discussed and shown to be directly related to the quench hardening in the transition zone. A comparison of this zone for welded 0.40 and 0.24% carbon steel shows that martensite and troostite, respectively, are produced. The lack of ductility combined with high residual stress is considered as responsible for cracking in the shop during fabrication after welding.

Quoting the author again, "The fact that fusion is initially partial in a welded joint is one of the most basic troubles in the welding process." It becomes apparent that cracking of welds is a complicated phenomenon in which stress concentration, combined with low ductility just in this region of stress concentration, is responsible. This is the part of the problem which W. G. Theisinger and W. L. Warner are concerned with, e.g., the most critical condition in the deposition of the first bead. Chapman shows, however, that there is still another requirement for an otherwise perfect weld; deformability during subsequent fabrication.

Hodge, James C. Impact Values of Weld Metal. AMERICAN WELDING SOCIETY JOURNAL, **13,** 12–17, Jan. 1934. Some values of Charpy-notch impact resistance of transition zone metal are shown to have better toughness than weld metal for multi-layer weld, and approach the high toughness of the parent metal.

Warner, W. L. Welding of Structural Nickel Steel. AMERICAN WELDING SOCIETY JOURNAL, **19,** 15–23, June 1934. The high hardening propensity of $3^1/_2$% Ni steels with high carbon of 0.30% or more is associated with cracking in transition zone. Preheating or reduction in carbon content are considered only remedies.

Underwood, C. M. and E. J. Ash. X-Ray and Welding—The Foundryman's Aid to Quality Steel Castings. Trans. A. F. A., Vol. XLIII, 481–510 (1936). The effect of the subsequent beads of a multiple pass weld in reducing the maximum hardness is shown for one grade of air hardening steel in Table 13:

Table 13

	1 Single Bead	2 Additional Bead Adjacent to First Bead	3 Third Bead Applied Over First Two	4 4th Bead Over 3
Max Vickers Hardness next to bead	579	469 318	384 287	270–280

This grade of steel with 0.39% C, 0.63% Mn, 0.22%, Si, 2.99% Ni is shown to be very susceptible to air hardening as evidenced by the high value of maximum hardness. The next value shows first the maximum hardness of the transition zone of the new bead deposited, and below, the effect of the second bead on reducing the hardness of the transition zone of the first bead, e.g., a drop from 579 to 318 Vickers Brinell in the second pass.

The hardenability is again related by experience, to the tendency for crack formation.

Rapatz, F. The Joint Welding of Alloy Steels. I. & S. Institute, Welding Symposium, Vol. II, pages 507–516. A considerable number of Plain C steels with Carbon contents from 0.14 to 1.06% and a Cr-Mo steel with 0.25% C, 1.0 Cr and 0.25% Mo were welded with (1) gas, using bare wire, (2) coated electrode, (3) cored electrode, (4) austenitic electrode and the hardness gradients for the transition zones were plotted. Among others the indications are that the heat affected area decreases in size in the above order of welding methods. All of the methods produced, in a 0.51% carbon steel, martensite areas in the transition zone except gas welding; the martensite being smaller in area for the cored than the coated electrodes. The martensite area in the austenitic weld transition zone is toughened by addition of Ni to the rod. The Cr-Mo steel does not show martensite in the transition zone when welded with the coated electrode. It is concluded that 0.40% C is the limit of weldability (based on sufficient toughness in the transition zone to prevent cracking) for arc welding of plain carbon steels. The author also considers any alloy or carbon steel is weldable with the gas torch with but few exceptions.

For the same strength as carbon steels, some of the alloy steels harden in welding to a lesser extent, and therefore the latter have a higher degree of weldability. In connection with a review of the notch impact and cold bend values of welds made with the cored electrode, it is noted that the notch impact value of the cored electrode weld is lower in general than coated electrode welds, although the bend angle of 60 degrees, average, shows good ductility. Critical reliance on the notch impact test is assessed as an obstacle to welding in that there appears to be no connection of fatigue with impact strength for steels of the same tensile strength.

Rolfe, R. T. The Varying Mechanical Quality of Fusion-Arc and Oxyacetylene Welds of Mild Steel Plate, and the Effect of Normalizing Thereon. I. & S. Inst. Welding Symposium, Vol. II, pages 539–552 (1935).

It is of interest to quote as follows from this author, "The most valuable conclusion, and not a surprising one, is that the more difficult it is to distinguish the exact position of the weld, the better the mechanical quality."

The author's Izod impact tests with notches 1.1 and 2.2 inches from the weld indicate that the parent metal is usually tougher 1.1 inches from the weld than at the weld or 2.2 inches from it, although there are cases where the 1.1-inch position shows a minimum impact. Some of the value of the results is lost due to the author's tests being made on a variety of mild steels of undetermined specific composition.

By averaging all the results the author shows that on the average an improvement over original toughness is obtained 1.1 and 2.2 inches from the weld, a still greater improvement resulting from normalization after welding.

Armstrong, T. N. Arc Welding of High Carbon and Alloy Steels. Trans. A. S. M., Vol. 24, 567–584, Sept. 1936. In some work on two steels (1) rail steel 0.52% C, 0.60% Mn and plate steel of 0.29% C, 0.26% Mn, 3.25% Ni, 1.35% Cr which was carefully carried through and reported in excellent detail, the author was especially interested in the effect of preheat on the transition zone. Since this paper is easily available to American

readers the facts established in the investigation are briefly summarized below.

1. In some preheating tests with various sizes of electrodes the apparent heat-affected zone area increases with preheat and with electrode diameter, while the penetration of the weld bead into the parent metal, as measured by the distance of the fusion line below the welded surface, is practically unaffected.

2. Hardness tests show that with increasing preheat temperature, the hardness of the bead and transition zone decreases progressively and cracking in the transition zone is entirely prevented by preheating to a high enough temperature (above 500° F.) The absence of cracking is associated in the author's experience with the disappearance of martensite in the transition zone.

3. Impact tests to determine toughness of the weld and plate steel away from weld indicated a loss of toughness for the weld increasing with preheat temperature. The plate itself exhibited a minimum toughness for preheat temperatures of 400°, 500° and 700° F.

4. Quenching tests made on the parent materials quenched from 870° C. (1600° F.) and reheated to the corresponding preheat temperature of the welded samples, resulted in microstructures which were quite different than structures in the weld transition zone. In the quench tests martensite is completely broken down in both steels by reheating above 0.400° F. (200° C.) while in the weld transition zone a form (type) of martensite is found in the structure of the plate preheated to 1250° F. (675° C.). In the rail steel 500° F. (260° C.) preheat is not sufficient to prevent martensite formation.

Muller, J. Weldability of Higher Strength Steels. *Z. V. D. I.*, **78,** No. 44, 1293–1294, Nov. 3, 1934.

A method of classifying steels as to weldability by means of tearability tests for thin plate welded in fully restrained condition. A jig developed by the author is shown which clamps the plate on either side of the weld, preventing expansion or contraction to take place during the welding operation. After welding, the plate is bent back and forth until rupture occurs, and the per cent of oxidized surface on the cross section is taken as a measure of the weld tearability. In good welds this is zero.

The test was used to map out a region of carbon content from 0 to 0.30% for a (P + S) content of 0 to 0.08% in which good, intermediate and poor weldability results. From 0.06% (P + S) and 0.2 to 0.3% carbon the welds are poor with a tearability rating of 10 to 80%. The intermediate weldability zone 0.05% (P + S) and 0.10% carbon to about 0.02% (P + S) and 0.30% carbon show a weld tearability of 3 to 10%. The zone of good welds from 0.04 to 0.05% (P + S) and 0.10% carbon to 0.015% (P + S) and 0.30% carbon show 0% tearability.

The steels of poor weldability showed marked liquidation zones and in the overheated zone the ferrite was characteristically dispersed in the large pearlite grains as small islands in the middle of which were inclusions which are considered to have acted as nuclei for the unusual ferrite precipitation.

For a clean steel, the upper limit of carbon for weldability is set at 0.27%. A good relationship was found to exist between per cent carbon and frequency of slag inclusions in the weld.

The cause of the high hardness of the weld is considered as the suppression of the secondary crystallization of the overheated zone due to alloying components.

Tests of hardness with Rockwell B, 3 mm. apart next to the weld bead were coupled with examination of microstructure and bending tests and the following conclusions made:

1. The hardness of the overheated zone should not increase more than 90 to 100 kg./mm.^{2} in strength.

2. A good weld should have no martensite next to the bead.

3. A good weld should bend at least 90° next to the bead.

4. Weld tearability and hardness are related, but their relationship is not exact.

Hoffmann, W. The Welding of Steel Tubes for Aircraft. *Z. V. D. I.*, **79,** No. 38, 1145–1148, Sept. 21, 1935. The author considers that steels above 0.35% C have too great a hardness increase in the transition zone which decreases the fatigue resistance. He states that the zone is brittle and cannot resist impact loads. He cites the work of J. Muller (*Z. V. D. I.*, **78,** 1293 (1934)) who sets limits of P + S content *vs.* C content below which the steels do not produce tears alongside the weld when welded in fully restrained condition. The author's observations lead him to the conclusion that tears do not occur in the absence of high O_2 or inclusion content of the steel, and furthermore, the Mn should be between 0.70 and 1.5% according to C content, which will prevent tears even with (S + P) up to 0.08% and O_2 above 0.008%. The steels in Table 14 are welding steels which are stated to be free from tears when welded.

Bollenrath, Franz and Heinrich Cornelius. Sensitivity to Weld Cracking of Steels of Higher Strength. *Stahl u. Eisen*, **56,** No. 20, 565-571, May 14, 1937.

The welding cracks considered are those for gas welding of thin plate or tubing that take place next and parallel to the weld bead, either during the welding or immediately after. The cracking tendency is greater with decrease in thickness of the plate or tube wall. Steels with less than 0.20% C are insensitive to crack formation, but at 0.35% C and above in unalloyed steels this condition is serious. The causes of crack formation have not as yet been determined with certainty, but several indications as to their cause have recently become available, among which are the internal stress due to the welding operation. It is shown that in tensile tests of various steels below, at temperatures of 0 to 1000° C. there is a range of maximum ductility at around 700 to 800° C. with the tensile strength at low value, thereafter the ductility decreases rapidly while the strength decreases slightly. J. Muller (*Luftf. Forsh*, **II,** 93, 103, 1934) observed that cracking takes place more readily in those steels which have a higher strength at room temperature, whereas all the steels shown in Table 15 have practically the same strength between 800° and 1000° C.

The first two steels were found to have no tendency for welding crack formation.

Table 14

C	Si	Mn	Ni	Mo	S	P	N	O	(S + P) max.
0.15	0.15	0.7–0.8			0.03	03	0.003	0.0055	0.07
0.25	0.30	0.7–1.0			0.025	"	0.005	0.0048	0.08
0.3 –0.33	0.45	0.8–1.2			0.025	"	0.004	0.006	0.07
0.3	0.2 –0.33	1.0–1.5	1.0–1.5	0.25–0.3	0.025	"	0.004	0.005	0.07

Table 15

Steel	C	Mn	Si	Ni	Cr	Mo
1	0.12	1.42	0.23		0.07	0.02
2	0.13	2.26	0.53		0.08	0.02
3	0.25	0.57	0.30		0.71	0.20
4	0.15	0.69	0.08	2.9	0.08	0.01
5	0.56	0.53	0.01			

The appearance of cracks in the microstructures of samples of the tensile tests made at 800 to 1000° C. is the same as in the cracks made in the welding operation; i.e., the cracks follow the grain boundaries and it is concluded that both types of cracking occur at approximately the same temperature.

The author considers, the criterion of weld cracking sensitivity on the basis of S and P content (J. Muller) to be overrated for plain C steels and inapplicable for alloyed steels; his opinion is that inclusion content of the steels may show better correlation with weld cracking.

Preheating of the plate or tube on both sides of the weld is advocated as the only positive means of welding a crack-sensitive steel.

In answer to some of the doubts as to the causes of tearing and the temperature range in which the phenomenon occurs, O. Werner gives a particularly apt discussion on pages 573–575.

Werner points out that J. Muller's original investigation showed that the question of weld tearability was associated by him not only with sulphur content of the steel, but with a sulphur content for a particular carbon content, and that the higher the carbon the less the allowable limit for sulphur. Werner further proves that an additional consideration is the method of producing the steels shown in the table given below comparing J. Muller's steels for sulphur tolerance for freedom from tears, with some steel specially prepared by "Manganese reduction."

Table 16—Sulphur Content Where Weld-tearing Starts

Carbon %	% Sulphur According to J. Muller	% Sulphur Specially Prepared Steels
0.15	0.035	About 0.050
0.20	0.030	" 0.043
0.25	0.025	0.035
0.30	0.020	0.030
0.35	0.015	0.025

In order to define the causes of the cracking observed by the above authors, Werner makes the following points:

1. The observed weld tearing is experienced principally in the welding of thin sheet between 0.5 to 3 mm. in thickness. The tears considered are those which are formed at high temperature, and which have the familiar blue color on the fractured surface. They should not be confused with the fissures which result from quenching stresses, structural embrittlement, etc., which occur also in considerably thicker welded sections.

2. In general, the weld tears occur only when hindered from contracting transversely. Unrestrained tubing and sheet also with un-ideal chemical composition may be joined by welding without difficulty, provided the bead length is not too large.

3. Solely from experience it has been determined that for a certain carbon content, the sulphur content must not rise above a certain value or else weld tearing results.

Further discussion by Werner indicates his belief that the effect of various factors on the initiation and delay of the gamma→alpha transformation is the most important consideration in assigning causes for fissuring.

In a steel in which for any reason the gamma → alpha transformation is delayed and takes place from 150 to 200° C. below the critical temperature, the stresses produced by the transformation may not be effective in promoting micro fissures (and thus macro fissures or tearing) due to the greater strength of the steel at the lower temperature. The effect of increasing sulphur content is regarded as promoting the initiation of the transformation and its consumation at a higher temperature where the strength of the steel is lower, and unable to resist the stresses set up without fissuring. The less the oxide and sulphide content, and the higher the manganese content, the greater is the delay in the transformation and the less the danger of fissuring; but as a natural consequence, the greater is the residual stress. The time of incubation and the lower A_3 temperature for the austenitic transformation is also to be considered for the difference in behavior of steels with increasing carbon content, although Werner considers the manganese content as far more effective for a lower A_3 and a delay in transformation.

Fiek, G. Testing Methods for Welds. *Ztsch. V. D. I.*, **77,** No. 37, 1005–1008, Sept. 16, 1933.

It is of interest to quote the following from this author: "The following questions apply to hardness testing:

1. Is the hardness test a suitable criterion for the correct choice of parent metal for welding and cutting?

2. Is the hardness test of value in determining the properties of the weld joint?

3. Do the hardness tests have a bearing on the other properties of the weld joint, e.g., tensile, ductility, wear and corrosion properties, etc.?"

Zeyen, K. L. Welding of Unalloyed Steels of Higher Strength. *Stahl u Eisen*, **56,** 654–657, June 4, 1936.

A series of seven steels of increasing carbon content was butt-welded with metallic arc and gas torch, as shown be-

Table 17—Hardness in Transition Zone

Steel	C	Mn	Si	Firth Hardness Original Hardness	Arc Welded Heavy Coated Electrode E52W. Firth Hardness in Transition Zone 1 Mm. Below Top of Plate	Lightly Coated 1.5% Mn Electrode (Same Firth Hardness Test)	Arc Welded Alloyed, Cored Electrode Firth Hardness 1 Mm. from Top	Heavy Coated, Austenitic Electrode 0.10% C, 25% Cr, 20% Ni Firth Hardness 1 Mm. from Top	Gas Weld Welding Rod 1% Cr, 0.20% Mo Firth Hardness, 1 Mm. from Top
1	0.11	0.47	0.12	91	133	133	148	142	167
2	0.17	0.55	0.31	130	149	150	164	180	170
3	0.30	0.56	0.23	152	190	184	187	235	170
4	0.38	0.64	0.29	165	209	191	224	250	201
5	0.56	0.63	0.32	180	226	229	260	291	244
6	0.60	0.63	0.33	203	305	282	278	291	266
7	0.68	0.69	0.35	210	355	298	292	312	272

low in the summary of hardness tests in the transition zone.

The tabulation given by the author is for 6 mm. and 12 mm. plates for which he gives tensile and yield strength, bending angle, Firth hardness, notch impact and strength on repeated bending for the parent materials before welding tests. Tests on the welded plate were for tensile strength with and without the reinforcement of the bead, bending angle under the same conditions. Hardness tests were made on the 12 mm. thick welded plates for weld bead and transition zone at depths of 1 mm. and 6 mm. below the top of the plate. Notch impact tests were made with the notch in the root of the weld.

Table 17 is a summary of the hardness tests made in the transition zone of the 12-mm. plates at a depth of 1 mm. from the top of the plate which the reviewer has taken out of the author's complete table.

The author's conclusions are that plain carbon steels are weldable with the metal arc when the electrode is suitably chosen even when the carbon is between 0.35% and 0.68%. The electrode most suitable for these higher carbon steels is considered to be the 1.5% Mn with a light coating.

Sandelowsky. Energy Balance in the Electric Arc. *Elektroschweissung.*, **5,** No. 9, 178–179, Sept. 1934. The following notes from this article are of interest in judging the heat effect in the parent metal. An approximate balance is given as:

Melting of electrode (bare electrode, presumably)	17%
Melting of scarf	9%
Radiation from arc	15%
Heating of the parent metal	29%
Conduction and radiation of parent metal	30%
	100%

The melting rate of the electrode in cm.³/min. is considered to be dependent on the parent metal and welding method. The author criticizes the setting up of constants such as the melting rate of electrode according to $K = \left(\frac{\text{cm.}^3}{\text{A. min.}}\right) = \frac{\text{bead volume for electrode}}{\text{current} \times \text{time of deposition}}$ and shows that the melting rate is higher for the same electrode when a bead is laid on top of a flat plate than when the deposit is made in a Vee groove both at the same speed of welding.

Another test is described to show the effect of welding speed on cm.³/min. melting rate and a peak value is obtained at 5 meters/hr. in both cases. This is explained on the basis of the heat loss due to absorption of welding heat by the parent metal. This becomes less with increase in speed since there is less time for heat conduction through the plate. On the other hand, the heat to melt the scarf increases and according to whether the first or second loss is greater the amount of electrode metal melted per minute either increases or decreases. The optimum value of melting rate is therefore the point where the total losses are a minimum. To increase melting rate further the energy of the arc must be increased or the losses decreased. The increase in arc energy is only possible by decreasing the length of conductor or by cooling the rod to decrease the resistance. Reduction of energy losses can be accomplished only by maintaining a shorter arc with a consequent lower radiation loss. The latter points to the profitable use of automatic welding.

It becomes apparent from the above that maximum heat effect as shown by maximum hardening is obtained with optimum melting rate, and therefore where the composition of steel is such as to require a lower welding speed to increase the heat affected area (thus decreasing the heat effect as shown by maximum hardening), the optimum melting rate for the electrode may not be obtained.

Owens, James W. Fundamentals of Welding (Penton Publishing Co. 1923).

One of the first continuous micrographs from the weld through the transition zone to unaffected parent metal made by A. G. Bissell is shown on pages 194–195 of this book. The areas are marked out to show the approximate temperature range for the micrographic features shown. On p. 224 the temperature of

Metal arc is given as	6000° F.
C arc	7000° F.
Oxyacetylene flame	4600° F.
Thermit reaction	5000° F.

See also: Suits, C. G. A Study of Arc Temperatures by an Optical Method. *Physics* **6,** 315–322, Oct. 1935, and High Pressure Arcs, *Gen. Elect. Rev.*, **39,** 194–200, April 1936.

Seferian, D. Study of Temperature of the Oxyacetylene Flame. *Rev. de la Sondure Autogene*, **26,** 6–7, July 1934.

Rapatz, Franz. Metallurgical Observations on Fusion Welding. *Stahl u Eisen*, **51,** 245–253, Feb. 1931. The first part of this paper concerns itself with weld metal, but there is included a comparative study of the extent of the heat affected zone for various welding methods in welding a 10-mm. steel plate. The comparison is made graphically on page 250, but is given by the reviewer in tabular form (Table 18):

Table 18

	Overheated Zone Mm.	Normalized Zone Mm.	Subcritically Annealed Zone Mm.
Bare rod (arc weld)	1.1	.8	.4
Coated rod (arc weld)	2.2	1.5	2.3
Bare rod (gas weld)	21.5	4.0	2.0

The author recognizes the possible existence of still another zone in which temperatures of 250° C. may have been attained. In this zone brittleness may exist due to aging.

Leitner, Franz. Additional Materials for Automatic Arc Welding. *Elektroschweissung.* **5,** No. 7, 121–127, July 1934. Some figures are given which are of interest, although not entirely applicable to heavily coated electrodes.

For a 5-mm. bare electrode 0.09% carbon, on 12-mm. thick steel plate at 200–220 amps., 17–20 volts.

Table 19

Welding Speed Meters/ Hr.	Welding Speed Inches/ Min.	Splatter Loss %	Penetration %	Melting Rate Grams/Min.	Heat Affected Zone
6	3.9	17.0	42.4	35.9	2.71 mm.²
12	7.9	16.8	46.9	36.3	1.41 "
18	11.8	16.6	40.3	35.6	0.94 "

The effect of arc length is shown as below, cored electrode 4 mm. diameter, 11 meters/hr. (7.2 in./min.) constant energy of 14.5 kw.

Arc Length	Current	Melting Rate Grams/Min.
3 mm.	230	49.2
7 mm.	210	47.7
13 mm.	185	41.5

It is noted that the bead becomes flatter, and the heat affected zone smaller with increase in arc length, the maximum hardness rises with increase in arc length.

Brennecke, R. Welding of Steels of Higher Strength. *Elektroschweissung*, **7**, No. 8, 151–155, Aug. 1936. The German steels St60, St70 are considered to be troublesome in welding due to the danger of crack formation in the weld and transition zone, in addition to the possibility of pore formation from the large amount of CO formed. The steels tested had carbon contents of 0.40 to 0.60% Mn 0.60–0.70 and Si 0.11–0.35 with low P + S in most cases. A special electrode was evolved, no details given, which gave satisfactory results with no cracking or fissuring. The microstructure showed no evidence of martensite formation. The best results were obtained with the special electrode when the multi-layer weld was carried out without allowing the lower bead to cool, the hardness contour in the latter case was less steep, showing a lower rate of quench from welding heat.

A quenching test to determine quench sensitivity of the material was made by quenching the welded section in water from 900° C. The micros show martensite in the parent metal transition zone, and in the weld, which is considered as indicating uniform hardenability, and it is noted that quenching cracks did not appear in any of the zones.

Erber, Günter. Materials and Methods in Built-Up Welding. I. & S. Inst. Welding Symposium, Vol. II, 89–96 (1936).

"The nature of the parent metal, therefore, has scarcely any influence on the liability to failure (in alternating stress). An exception to this statement occurs in the case of steels subject to crack formation on sudden heating (such as high alloy steels with consequent low heat conductivity), or in the case of steels having a high coefficient of thermal expansion in these instances the welding process may cause a crack in the parent metal, which may reduce its strength so as to cause a fracture even if the welding is absolutely perfect."

On the basis of obtaining a high notched bar fatigue value, it is recommended that the parent metal be chosen with lowest possible O_2 content and a low Si content.

Hanemann, H. and A. Hilpert. Metallographic Investigation of Arc. Welds. *Elektroschweissung*, **3**, No. 6, 101–108, June 1932.

The paper deals mostly with the weld metal, but is of interest as being among the first to discuss and show in micrographs the effect of laminations in producing cracks in the heat-disturbed zone starting from the fusion line. The transition zone is divided into three zones: (1) overheated, grain growth region; (2) recrystallized, fine grain zone and (3) zone of carbide change without grain disturbance. It is noted and shown in several micrographs that a banded structure in the parent metal becomes homogeneous only adjacent to the fusion line, and the heat disturbed zone is a banded one for the greater part of the first or overheated zone.

The transition zone is considered to be any part of the parent metal that has been heated to 700° C. or higher.

Block, E. and H. Ellinghaus. The Free Bending Test. *Elektroschweissung*, **4**, No. 7, 126–129, July 1933.

In a discussion of the merits of the free bend, *vs.* the restrained bending over a mandril, it is shown that the free bend test can be used to determine the deformability of the weld, transition zone and parent metal of a welded plate. By measuring the elongation progressively across the welded section, the zone of lowest strength is indicated as the zone of greatest elongation.

Haardt, Erich. The Composition of Arc Weld Deposits and Dependence on Parent Metal, Electrode and Welding Method. *Elektroschweissung*, **5**, No. 9, Sept. pages 161–164, Oct. pages 193–196 (1934).

A series of 3½% Si, Mn, Cr, Ni and 1½% Cu steel plates was welded. The weld plates were 160–100 × 20 mm. which were welded with a mild steel electrode. Loss by diffusion was found to be greatest for Ni and Cu; for Mn and Si the loss is considerable, but is due to mixing in the fluid state, and not to diffusion.

Mladiata, A. J. Structural Changes in Mild Steel Welding and Effect on Strength of Ship Members. *Werft, Reederei u. Haafen*, **17**, 401–402, Dec. 15, 1936. Butt, Vee welded 10 mm. thick plate material with 0.17% carbon welded with dipped (lightly coated) metal arc. 0.09% carbon showed.

Table 20

	Tensile Strength	Yield Strength	Elongation
Parent	43 kg./mm.²	28.7 kg./mm.²	21% (200 mm.)
Welded	48.5 "	34.5 "	15% (100 mm.)

It is of interest to note that the author states the lightly coated electrode is to be preferred to the heavily coated rod, due to the too great penetration, larger residual stress as a consequence of the greater heat effect of the latter electrode. It is also noted that the author writes of a high nitrogen content of the weld metal from the first type of electrode above.

There is just one other important value in this paper, that for impact resistance of the heat disturbed zone. From the heat-affected zone under the fillet bead joining an angle stiffener to a plate he has cut out some impact samples for tests as given below:

	Impact in mgk./cm.²		
In heat-affected zone	11.3 (as-welded)	10.1	after 920°—2 hrs. and
Parent metal	12.1	10.4	slow cool-treatment

The structure of the heat-affected zone is shown to be large grained with suggestion of the Widmanstaetten pattern, the carbide being well dispersed.

Texter, C. R. and F. N. Speller. The Quality of Materials for Fusion Welding. A.S.T.M. Symposium on Welding, Pittsburgh 1931, pages 29–41.

The following is quoted to close the review, and to show that the papers, such as are reviewed above, which are accumulating at an accelerated rate, are in answer to the need expressed here for defining the weldability of steels.

"It is not possible to definitely define all the factors which influence weldability, and the statement in some specifications that steel shall be of good weldable quality leaves the manufacturer in somewhat of a quandary."

A review of chemical and some physical specifications is given which were in use at this time for steels which were successfully welded. The carbon limit was set at 0.30% by the A.S.T.M., while the authors show successful welds in steels with higher carbon.

Supplementary References

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Rosenthal, D., *L'Usine*, **41**, 31, Feb. 5, 1932.

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Portevin, A. The Scientific Fundamentals of Autogenous Welding. *Bull. d. l. Soc. des Ing. Soudures*, **4**, 901 (1933).

Bornefeld, H., *Tech. Zisch. f. praktische Metallb. No.* ¹/₁, ³/₄, 14–52 (1933) (supplement to *Praktische maschinenkonstrukteur*).

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Variot, E. Study of the Internal Strains in Welds. *Arts et Métiers.*, **37**, Jan. 1934.

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Seferian, D., *Rev. de l'ind. Min.*, No. 341, 100–112, March 1, 1935.

Holler, H., and Frankenbusch, H., *Autog. Metallb.*, **28**, 337–344 (1935).

Siemers, K. Principles and Application Possibilities of the Various Electric Welding Methods. *Elektrowarme*, **6**, 230–237, Aug. 1936.

Discussion on Fusion Welding Wrought Iron

By B. D. LANDES*

IN CONNECTION with the review of literature on Fusion Welding Wrought Iron some interest may attach to tests carried out in 1935–1936 for the American Bureau of Shipping and Lloyd's Register. The tensile properties and chemical analysis of weld metal deposited from ordinary coated mild steel electrodes in wrought iron plate were determined using standard specimens. The plates were 30 x 6 inches, 1 inch thick, joined on the long dimension, the fiber direction in the wrought iron being paralleled to the short dimension, that is, perpendicular to the weld. The weld was 80° V, $^{3}/_{32}$-inch shoulder. The first two beads were run with a $^{5}/_{32}$-inch electrode, 26 volts, 135 amps. The remaining 17 beads were run with a $^{3}/_{16}$-inch electrode, 29 volts, 185 amps. In one series of tests both plates were wrought iron; in the other series one plate was wrought iron, the other plate was steel.

The composition of the deposited metal in both series of tests is shown in Table 1. The weld metal picked up some phosphorus and silicon from the wrought iron.

The properties of the deposited metal obtained in both series of tests are listed in Table 2. The results more than fulfill the requirements of both the American Bureau of Shipping and Lloyd's Register, for whom the tests were made.

In bend tests of butt-welded joints, as contrasted with all-weld-metal, cracks usually appear adjacent to the weld metal in the more ductile wrought iron. The elongation is localized to some extent in base metal on account of its lower yield strength.

Three series of tests were made to determine the effect of plate thickness and direction of slag fibers in single V butt welds in wrought iron. In Series 1, Table 3 wrought iron was welded to wrought iron, the slag fibers being perpendicular to the weld. That is, the slag fibers were parallel to the axis of load, for in all tests the weld was perpendicular to the axis of load. In Series 2, Table 4, wrought iron was welded to wrought iron, the slag fibers being parallel to the weld and, consequently, per pendicular to the axis of load. In Series 3, Table 5, one plate of the welded joint was wrought iron, the other plate was steel. The fiber in the wrought iron plate was perpendicular to the weld. The preparation of the plates is shown in Table 6. The coated electrodes and deposited metal have already been described.

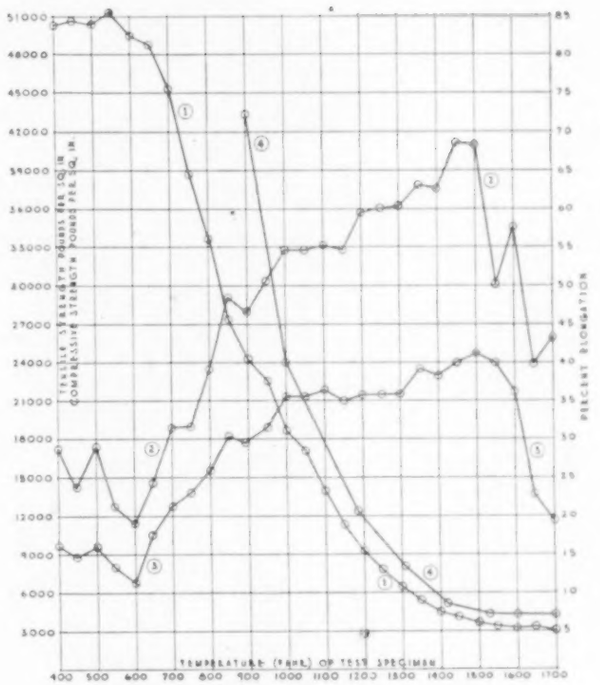

Fig. 1—Properties of Wrought Iron at Elevated Temperatures
Curve 1 = Tensile strength, lb. per sq. in.
Curve 2 = Per cent elongation in 1 inch
Curve 3 = Per cent elongation in 3 inches
Curve 4 = Ultimate Compressive Strength, lb. per sq. in.

Table 1—Composition of Metal Deposited from Coated Mild Steel Electrode in Wrought Iron Plates

	C	Mn	P	S	Si
Electrode	0.15	0.41	0.003	0.021	0.002
Deposit between two wrought iron plates	0.07	0.29	0.041	0.018	0.098
Deposit between one wrought iron and one steel plate	0.06	0.34	0.026	0.020	0.104

Table 2—Properties of Metal Deposited from Coated Mild Steel Electrodes in Wrought Iron Plates

	Deposit Between Two Wrought Iron Plates, 1 Inch Thick	Deposit Between One Wrought Iron and One Mild Steel Plate, Each 1 Inch Thick
Tensile strength, psi	64,900	67,900
Yield strength, psi	56,670	57,910
Elongation, % in 2 inches	34	22.5
Reduction of area, %	54.7	43.7
Izod notch impact value, ft.-lb.	75	79
Specific gravity	7.841	7.838
Bend test	180° without fracture on a diameter less than the thickness of the specimen	135° without fracture on a diameter euqal to the thickness of the specimen

The unwelded wrought iron was tested with slag fibers parallel to axis of load in Table 3.

The unwelded wrought iron was tested with slag fibers perpendicular to axis of load in Table 4. No requirements have been set up by Lloyd's Register or American Bureau of Shipping for the transverse properties of wrought iron. Byers specifications require 36,000 psi tensile strength, 27,000 psi yield strength, 2.0% elongation in 8 inches.

The American Bureau of Shipping requires that wrought iron should develop 48,000 psi tensile strength, 12% elongation in 8 inches with slag fibers parallel to axis of load. A deduction of 1% elongation is allowed for each $^{1}/_{16}$ inch of thickness below $^{7}/_{16}$ inch. The requirements of Lloyd's Register for wrought iron (fiber parallel to load) are 48,200 psi and 12% elongation in 2 inches. A. S. T. M. and Byers specifications allow a deduction in tensile strength of 1000 psi for each $^{1}/_{8}$ inch thickness over $^{3}/_{4}$ inch, but not less than 39,000 psi for wrought iron with fibers parallel to axis of load.

In every test the weld developed considerably more than 90% of the strength of unwelded wrought iron of the same thickness. Since the yield strength of wrought iron is less than that of steel most of the elongation in the composite specimens of Table 5 occurred in the wrought iron.

* Manager, Engineering Service Department, A. M. Byers Company.

Table 3—Welding Details and Tensile Properties—Series 1—V Weld in Two Wrought Iron Plates; Slag Fibers Perpendicular to Weld

Bead Number	Diameter of Electrode, Inch	Volts	Amperes
	Plates 1 inch thick		
1 and 2	5/32	27	125
3 to 14 inclusive	3/16	30	190
	Plates 3/4 inch thick		
1 and 2	5/32	28	125
3 to 12 inclusive	3/16	30	185
	Plates 1/2 inch thick		
1 and 2	5/32	28	130
3	3/16	30	185
4 and 5	3/16	30	175
	Plates 1/4 inch thick		
1 and 2	1/8	28	90
3	5/32	27	125

Tensile Test Results

Plate Thickness, Inch	Tensile Strength, psi		Yield Strength, psi		Elongation % in 12 In.	Elongation % in 8 In.	Elongation Welded % in 8 In.
	Unwelded	Welded	Unwelded	Welded	Unwelded	Unwelded	Welded
1	44,800	45,120	27,540	26,650	16.5	18.0	14.0
3/4	47,440	48,360	26,420	27,860	19.4	21.5	16.0
1/2	49,130	48,570	29,790	29,080	17.8	19.0	16.5
1/4	53,870	52,720	40,840	41,630	13.1	14.5	12.0

Table 4—Welding Details and Tensile Properties—Series 2—V Welds in Two Wrought Iron Plates; Slag Fibers Parallel to Weld

Bead Number	Diameter of Electrode, Inch	Volts	Amperes
	Plates 1 inch thick		
1 and 2	5/32	27	125
3 to 14, inclusive	3/16	30	190
	Plates 3/4 inch thick		
1 and 2	5/32	28	125
3 to 12 inclusive	3/16	30	185
	Plates 1/2 inch thick		
1 and 2	5/32	28	130
3	3/16	30	175
4 and 5	3/16	30	185
	Plates 1/4 inch thick		
1 and 2	1/8	28	90
3	5/32	27	125

Tensile Test Results

Plate Thickness, Inch	Tensile Strength, psi		Yield Strength, psi		Elongation, % in 8 Inches	
	Unwelded	Welded	Unwelded	Welded	Unwelded	Welded
1	36,980	35,040	26,230	25,370	7.0	6.5
3/4	43,570	43,540	26,350	26,740	10.5	7.5
1/2	40,770	42,260	27,950	27,870	7.0	8.0
1/4	46,960	45,040	38,580	39,460	7.0	5.0

The properties of fillet welds on wrought iron were determined on a specimen consisting of two pieces of wrought iron 3/4 inch thick, 10 x 2 inches, with the fiber parallel to the long dimension, side fillet welded to two pieces of steel 1 inch thick, 8 x 3 inches. The wrought iron specimens were lapped on either side of the two steel specimens for a distance of 3 inches on each end, thus leaving a gap of 4 inches between the ends of the two steel sections. The specimen had a breaking load of 160,830 lb., the test requirement being 147,840 lb.

These tests provide practically the only information available on the strength of fusion welds in wrought iron made in this country They confirm the excellent results secured abroad.

Several topics in the review attract discussion. For example, the impression is conveyed that wrought iron is not well suited to resistance welding. However, if the correct procedure is followed there is no difficulty whatever in resistance welding wrought iron. Wrought iron contains slag and in joining lengths of pipe by the resistance process there is a tendency for the slag fibers which are parallel to the axis of the pipe to be upset slightly. Consequently transverse structure is obtained especially on the outer edges of the sections. To minimize the extent of transverse structure excessive pressure should not be used in forcing the edges together. The pressure should be just great enough to produce sound union.

Table 5—Welding Details and Tensile Properties—Series 3—V Welds Between One Wrought Iron and One Steel Plate; Slag Fibers in Wrought Iron Perpendicular to Weld

Bead Number	Diameter of Electrode, Inch	Volts	Amperes
	Plates 1 inch thick		
1 and 2	5/32	27	125
3 to 19 inclusive	3/16	30	190
	Plates 3/4 inch thick		
1 and 2	5/32	28	125
3 to 12 inclusive	3/16	30	185
	Plates 1/2 inch thick		
1 and 2	5/32	28	130
3	3/16	30	185
4 and 5	3/16	30	175
	Plates 1/4 inch thick		
1 and 2	1/8	28	80
3	5/32	27	125

Tensile Test Results

Plate Thickness, In.	Tensile Strength, psi		Yield Strength, psi		Elongation Unwelded % in 12 In.	Elongation Steel % in 8 In.	Elongation Wrought Iron, % in 4 In.	Elongation Weld Average % in 8 In.	Elongation Steel % in 4 In.
	Unwelded	Weld	Unwelded	Weld					
1	62,790	44,930	31,630	26,600	28.0	33.0	18.0	10.5	2.0
3/4	62,720	47,940	35,340	27,160	25.5	30.0	25.0	15.5	4.0
1/2	61,670	49,880	37,690	32,370	27.9	32.5	21.5	15.0	3.0
1/4	64,230	54,400	41,720	42,290	26.9	31.0	18.0	14.0	6.0

Table 6—Preparation of V Welded Wrought Iron Plates

Plate Thickness, Inch	Angle of V, Degrees	Tongue, Inch
1/4	60	None
1/2	60	1/16
3/4	80	3/32
1	80	3/32

Excellent service has been obtained from an installation of 21 miles of 1-inch wrought iron pipe with resistance welded joints laid in 1925 in an ice skating rink in Pittsburgh. Other installations of a similar kind have since been made.

Recently we have determined the tensile properties of Aston process wrought iron at temperatures from 400 to 1700° F. (200 to 930° C.). The results are shown in Fig. 1, and supplement the less complete information given in the Appendix to the review.

Table 7—The Maximum Manganese Content of High-Quality Wrought Iron Permitted by A. S. T. M. Specifications

A. S. T. M. Specification Designation	Product	Maximum Manganese Content, %
A 42-36T	Wrought iron plates	0.06
A162-36	Wrought iron sheets (black)	0.05
A163-36	Wrought iron sheets (galvanized)	0.05
A152-36T	Wrought iron rivets	0.06
A189-36T	Refined wrought iron bars	0.06
A 72-33	Wrought iron pipe	0.05
A 73-36	Wrought iron blooms and forgings Class A	0.07
A 73-36	Wrought iron blooms and forgings Class B	0.15

In conclusion it may be pointed out that A. S. T. M. specifications for high-quality wrought iron restrict the manganese content to 0.05 to 0.07%, Table 7. In A. S. T. M. A73-36 Class A wrought iron is for railroad and miscellaneous uses and is free from admixture of iron or steel scrap other than wrought iron scrap from the manufacturer's stock, which is from the same product and of equal quality. Class B is for other than railroad car and locomotive purposes and is made from selected No. 1 wrought iron scrap.

Low manganese is one of the characteristics of good quality wrought iron. A. S. T. M. A85-27 for common iron bars refers to a bastard material made up of scrap of undefined character. This specification can not be considered to refer to wrought iron.

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WELDING RESEARCH COMMITTEE

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RESEARCH SUPPLEMENT

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Translations

The following translations are available for distribution:

1. Fundamental Researches on Welding Heat in Arc Welding Low-Carbon Steel.

Report 1. Theoretical Temperature Distribution During Welding.

Report 3. Effects of Welding Heat on Deposited Metal.

Report 4. Effects of Welding Heat on Base Metal.

These reports were published in 1936 by Professor Minoru Okada, Department of Metallurgy, Osaka Imperial University, Japan. The translation was made available by Professor N. F. Ward, University of California.

2. The Resistance Welding of Light Alloys by Dr. H. Portier, published in *Pratique des Industries Mecaniques*, **20**, No. 1, pages 3 to 10, April 1937.

General review of the factors involved in spot-welding aluminum and its alloys.

3. Spot Welding Light Metals, by E. Osswald of the German Aircraft Experiment Station (DVL); published in *Luftfahrtforschung*, **14**, 215–223, April 20, 1937.

(Structure, corrosion resistance, static and fatigue tensile strength of spot welds in aluminum containing 7% magnesium and in clad duralumin. Welds were made by tube controlled machine at constant current.)

4. The rigidity of Welded and Cast Machine Beds, by F. Koenigsberger; published in *L'Industria Meccanica*, **19**, 512–517, July 1937.

(Nomographs relating dimensions of the bed to weight, modulus and critical frequency. The dimensions of the cast-iron bed are limited by foundry factors, which accounts to some extent for the superiority of the welded bed from the standpoint of design.)

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The Relation of Research to Teaching and to Industry*

By W. M. WILSON†

There is considerable discussion relative to the effect of a man's research activities upon his ability as a teacher. The question is, Does engaging in research make a man a better or a poorer teacher? There is a wide divergency of opinion on this question due, I believe, to an equally wide divergency of opinion relative to the function of a teacher. What I have to say is the outgrowth of experience in university teaching, an experience limited almost entirely to graduate and advanced undergraduate students. This background should be considered when listening to what I am about to say.

To me, the one outstanding function of a teacher is to develop in the student a desire to learn. Desire is the most effective motivating force known to man. We may be compelled by fear or persuaded by a sense of duty, but activity based on desire is spontaneous and, except as interrupted by fear or duty, continues so long as desire lasts. Acquiring the ability to learn and the habit of thinking are the most valuable results of an education. The knowledge that a student acquires in college, relative to the knowledge required for success in industry or the professions, is as a drop in a bucket. If he has not acquired methods of learning and habits of thought, his college residence will have been largely in vain.

Let me repeat. The one outstanding function of a teacher is to develop in the student a desire to learn. Love of knowledge is the beginning of education.

A teacher, to inspire in the student a desire to learn, should himself be a creative scholar. The inspiration of accomplishment is a potent force. Success in any line is an effective stimulant. It creates in others a desire to succeed. Moreover, it is axiomatic that a teacher should know his subject. A music teacher must be a musician, and a teacher of mathematics must be a mathematician. Likewise, a teacher of creative thought must himself be a creative thinker.

Advanced students should participate in creative thought, and the research work should be genuine. Simulating research by solving problems that have already been solved by others may develop technique and may be intensely interesting. Moreover, it may develop a love of knowledge. But such work should be supplemented by genuine research, the solution of problems, however small, to which neither the teacher nor any one else knows the answer. Otherwise, the spirit of intellectual adventure will be lacking. The student must experience the groping, the failures, the continued effort without apparent success, the receptive attitude of the conscious mind for suggestive glimpses from the subconscious mind, the alertness for significant phenomena, the consuming interest that will make continuation of the work more important than eating or sleeping and finally, he must experience the exhilaration of success. All of these go into the training of a creative thinker. For the first time he will experience the satisfaction of collecting facts and arranging data so that their logical organization leads to a definite conclusion independent of any contribution from any one except himself and his teacher. It is a new experience in his intellectual life, and it enlarges his conception of his possibilities.

Developing creative thinking is expensive teaching, partly because the number of creative thinkers among teachers is small so that their salaries are likely to be greater than that of the average teacher, partly because the ratio of students to teacher is likewise small for this type of teaching, and partly because, to retain his proficiency, the teacher must continue his research work. And this latter requires a large amount of his time and it requires library or laboratory facilities or both.

Although developing the power of creative thinking is expensive, it is also profitable. Leadership in industry as well as in art and science is limited to those capable of creative thought. I have often stated that, for a student with an intellectual capacity which will enable him to profit by it, one year of graduate work is as beneficial as four years of undergraduate work. This is a conclusion I have arrived at after observing the degree of success attained by men with baccalaureate and men with graduate degrees.

One of the reasons why graduate teaching is expensive is that for a graduate teacher to retain his proficiency, he must

(*Continued on page 64*)

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Shrinkage Stresses in Welding
A Review of the Literature to January 1, 1937

By W. SPRARAGEN* and G. E. CLAUSSEN**

This report is prepared under the auspices of the Literature Division of The Engineering Foundation—
Welding Research Committee

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CAUTION.—There is no standard method for measuring residual stresses. The different methods that have been used for measuring residual welding stresses are known to differ in accuracy and in character of results. As usually applied, the Mathar and X-ray methods have indicated higher residual stresses than other methods. Residual welding stresses are not constant for a given technique of welding, but vary with thickness of plate, shape of joint, type of metal, cooling rate and other obvious variables. Several topics not dealt with in the present review have been summarized in the reviews on Shrinkage Distortion *and* Temperature Distribution During Welding.

SUMMARY

In fusion welding of low-carbon steel 1/2 inch thick or more without preheating, postheating or peening, there is invariably introduced residual stresses approaching the yield point of the material perpendicular to the weld and at short distances (2 inches or less) therefrom. The stresses parallel to the weld are even greater as the biaxial, and in very thick plates triaxial, stresses apparently raise the yield point 30% or more.

A great deal of caution is necessary in the interpretation and translation of strain gage measurements in terms of residual stresses. In many instances these measurements indicate that permanent deformation has taken place with consequent reduction of residual stresses to at least the yield point. In some instances incipient failure (microscopic) may also have taken place.

A method of determining residual stresses in welds that is NOT SAFE is the "direct" method in which measurement is made of the change caused by welding in the distance between two gage points on a plate.

While the above factors are generally appreciated in comparisons of strain gage readings taken before and after welding, the same precautions must be followed in the interpretations of results obtained by some of the methods which measure the "relief of stresses" after welding.

As to whether or not these stresses are harmful or what happens to them in service is another matter which cannot be completely answered at this time.

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Butt Joints

Oxyacetylene Welds Without Restraint.—As in arc welds we find the stresses parallel to the weld to be less if the step back method is employed instead of the continuous method, but here again the stress perpendicular to the weld will generally be greater than in continuous welding. Moreover, in continuous welding the stresses parallel to the weld are greater than those perpendicular to the weld. In both instances the maximum stresses are less in most cases for gas welding than they are for arc welding the same thickness.

Oxyacetylene Welds with Restraint.—(*Circular Disks*) For the sizes and type of specimens that were tested, right-hand welding and mild steel gave less residual stress than left-hand welding and low-alloy steel, but these may not be general rules. The magnitude of residual stress depends to a preponderating extent on the relative volumes of elastic and inelastic material in the specimens during welding. The greater the restraint exerted by the elastic material, the higher is the residual stress. Preheating and peening reduce the maximum stress to two-thirds of the value of unpreheated and annealed.

(*Flat Plates*) In restrained welds stresses perpendicular to the weld approach the elastic limit of the plate and are much larger than the stresses parallel to the weld.

Arc Welds Without Restraint.—Procedure. The step-back method results in stresses parallel to the weld which have been found by one investigator to be 25 to 50% less than those which prevail with continuous welding (layers either in same or opposite directions), although the stresses perpendicular to the weld and in its immediate vicinity are increased considerably. (In a private communication Dr. Bierett stated that more research is needed before the specific effect of back step welding on shrinkage stresses is known.) These maximum parallel stresses generally occur at the center of the weld. The maximum stresses perpendicular to the weld may be either tension or compression, and their locations are dependent on a great many factors involving the geometry of the plate and joint, procedure, speed of weld and items of a like nature.

Type of Joint.—As is to be expected the single V weld with the relatively larger amount of weld metal for a given thickness of plate will result in a greater amount of residual stresses perpendicular to the weld than the X weld.

Coated Versus Bare.—Although reported results are somewhat contradictory, there does not seem to be any great difference between the maximum residual stresses parallel or perpendicular to the weld in bare wire welds as compared with covered although the stress gradient may be somewhat steeper with the bare, other factors such as energy input being the same. (The energy input is usually greater for covered electrodes than for bare.)

Arc Welds with Restraint (*Straight Butt Joints*).—One investigator found that the maximum reduced stress occurred in the weld (25,300 psi), but was below the yield point as determined in the ordinary tensile test. The average stress was:

21,600 psi in the weld
16,800 psi in the heat-affected zone as revealed by temper colors
9,700 psi or less in the remainder of the 0.47-inch plate.

Comparing the results of the arc welds made with and without restraint, he found that the stress distribution in the weld and its vicinity is not very different. In both cases the maximum tensile stress occurred in the weld and was parallel thereto; this stress was 23,000 psi in the constrained weld, which was smaller than that found in the unconstrained. On the other hand, the average tensile stress in the constrained weld and perpendicular thereto was 18,400 psi, which was a little larger than the corresponding stress in the free plates.

Another investigator found the residual stresses parallel to the weld were near the yield point of base metal, but considerably below the yield point of weld metal. There was practically no difference between these stresses in the free and restained weld, and the stresses were practically independent of sequence and root spacing. The maximum residual stress parallel to the weld always occurred at the middle of the joint.

Reaction stresses (stresses due to load perpendicular or radial to a weld and acting over the entire restrained length of a welded joint made under restraint) are less in some alloys as compared with mild steels. Reaction stresses are not related to the time to deposit the weld, or to depth of penetration, and are decreased by any treatment tending to produce a uniform cooling rate, whether fast or slow, in weld and plate. As the degree of rigidity increases reaction stress increases. Peening cold or hot decreases the amount of reaction stresses.

Arc Welds with Restraint (*Patches*).—Peening decreased the residual stresses by 40 to 50% and has greater effect with covered than with bare electrodes. Peening the hot weld relieves the elastic stresses by causing plastic strain; only the first layers should be peened. Peening did not appear to change the type of residual stress distribution. Water cooling the plates during welding increased residual stresses by increasing the temperature gradient.

On the average, back step welding does not reduce tangential or radial residual stresses in weld and plate. Low-alloy steels generally have up to 50% higher stresses than mild steel with equivalent treatment. Pre-bending a patch before welding decreased residual stresses by 16%.

Preheating.—Butt welds (V) in mild steel plates 8 inches square, 2 inches thick, fillet welded to a rigid foundation broke loose before welding was completed if not preheated. If preheated to 300° C., the joint did not break loose from the support, and showed no distortion after it was chipped from the support.

Physical Tests.—Tensile, bend and notch impact specimens cut from oxyacetylene or arc-welded butt joints with restraining "side frames" had the same properties as specimens cut from oxyacetylene and arc welds made without restraint.

Fillet-Welded Joints

Residual stresses are generally tension perpendicular to a double fillet lap joint and compression parallel to the weld. Under restraint the perpendicular stresses are only about half of those of equivalent butt welds.

T Joints

The welds are generally in tension and the adjacent parent metal generally in compression. Residual stresses seem to be of a lower order than that encountered in butt joints, or even lap joints.

Welded Beams

In welding flanges to webs with fillet welds residual stresses decrease as flange width is increased. In a T beam with double-V butt-welded web, high compressive stresses are induced in the tension area of the web.

The distribution of stresses parallel to the weld in the web of a welded T beam is non-uniform and may reach high values near the weld in both web and flange although the stresses are more uniformly distributed in the flange.

There seems to be little choice from a residual stress view-point (but not from a stress concentration view-point) between fillet welds and butt welds in the fabrication of T beams. Nose profile flanges seem to help in reducing residual stresses. There is no basic difference between the magnitude of residual stresses in rolled and welded beams. Residual stresses are higher in higher strength alloy steels than in mild steel beams.

Since alloy steel girders may be made in thinner sections than the equivalent mild steel girder, the apparent advantage of mild steel from the standpoint of residual stress is largely offset in practice.

In general, a reinforcing plate welded to the web of a riveted girder induces tensile stress (15,000 psi) in the added plate and compression (4500 to 9000 psi) in the original plate. Extensometer readings on plate-girder railway bridges strengthened by arc welding showed that, unless correct welding sequence was adopted, the maximum stress in the strengthened bridge under a given load may exceed that in the unstrengthened bridge.

Welded Boilers and Other Cylindrical Vessels

The residual stresses are maximum along the welds. The stresses are generally higher in the longitudinal seams than in the circumferential. Torch annealing is not effective in greatly reducing residual stresses of drums 30 inches in diameter and $^1/_2$ inch thick. Breathing tests on drums reduce the residual stresses.

In circumferential welds the stresses at right angles and parallel to the weld are not greatly different (16,000 *vs.* 17,000 psi). In the longitudinal welds the circumferential stress may be low (4500 psi) whereas the stresses parallel to the weld may be high (32,000 psi).

Beads Deposited on Surface

The residual stresses (and/or distortion) depend upon the amount of metal deposited, its location with reference to edge, the size and thickness of the material and the general stiffness of the section. Beads on both sides of a surface create higher residual stresses than beads on one side, especially if the second bead is deposited on a cold specimen. The maximum tensile residual stress occurs in the immediate vicinity of the bead. The second bead contains about 75% as much residual tensile stress as the first bead.

In building up worn shafts of 0.6% C steel by welding the stresses are greatest next to the built-up layer in the "hard zone." The residual stresses decrease as the carbon content of the electrodes approaches that of the shaft, possibly on account of the effect of carbon on the coefficient of thermal expansion.

Beads Deposited on Edge of Plate

In mild steel plate (0.18 C, $^3/_8$ inch thick) the welded edge and its vicinity (about 1 inch) at room temperature are stressed to the tensile yield point parallel to the edge. The unwelded edge is also in tension, the intermediate region being in compression to preserve equilibrium.

If welds are deposited on two opposite edges of the plate, both edges and their vicinity are stressed to the yield point in tension after the specimen has cooled to room temperature. The intermediate region is subjected to a constant residual compressive stress of about 17,000 psi. The change from maximum tension to maximum compression occurred in a distance of slightly less than an inch. A considerable change in the residual stress in a strip cut from a welded plate can be effected by annealing at 650° C. in a non-oxidizing atmosphere. Perpendicular stresses may in some cases reach 15,000 psi.

Flame-Cut Plates

There is no reliable quantitative information on residual stresses caused by flame cutting. Preheating is essential in avoiding cracks in flame-cutting high-carbon plate. Cracks are not encountered in steel containing less than 0.35% C.

Local Heating by Torch

Residual stresses in mild steel plates heated locally to relatively low temperatures may attain high values depending on the degree of localization of the heated zone.

Artificial Cooling

It is well known that shrinkage distortion in welding can be reduced by artificially cooling the vicinity of the weld. Although it may reduce distortion, artificial cooling may or may not have a bad effect on residual stresses, apart from the question of its bad effect in promoting the formation of brittle martensite in many steels.

Preheating

Preheating is said to prevent a good deal of distortion in welding heavy castings. There is practically no quantitative information on the effect of preheating in preventing distortion but it is certain, as pointed out in the Reviews on Cast Iron and Cast Steel, that preheating may reduce the tendency to cracking, provided the preheating temperature does not coincide with a brittle range of the material to be welded. Since stress together with ductility and stress distribution is one of the important contributing factors to cracking, it may be taken as axiomatic that preheating reduces residual stresses if the type of cooling from preheat temperature is favorable.

Stress Relief by Annealing

Stress annealing is effective in lowering residual stresses caused by welding. The amount of residual stresses remaining depends primarily on the temperature, assuming that the specimen is held sufficiently long at a given temperature to permit "creep" or plastic flow. Holding at about 1200° F. for at least an hour per inch of thickness reduces the residual stress to less than the short time yield point of the particular (mild) steel at that temperature (because of "creep"). For mild steel the resultant residual stress after stress annealing at 1200° F. amounts to not over 4000 to 6000 psi. Local torch annealing is of no value for relieving residual stresses in oxyacetylene welded drums, whereas thorough annealing gives complete relief.

It would appear that, for welds in low-carbon, unalloyed and low-alloy steels, in the absence of martensite in the original weld and heat-affected zones, stress annealing at ordinary temperatures (600° C.) for the usual times may have a negligible effect on the microstructure, provided effects due to precipitation of copper or other constituents are excluded. If the stress annealing is prolonged (heavy masses) or at a relatively high temperature, some agglomeration of carbide may be expected. Stress relieving a martensitic weld at 600° C. will cause marked tempering, provided the steel is not highly alloyed.

Stress annealing has little or no effect on the Rockwell hardness of bare or covered wire weld metal deposits, but in certain low-alloy steels ($3^1/_2$ Ni, 0.30 C type) more than 50% reduction in hardness has been found. On the other hand, it has been found that stress annealing (3 hrs. at 650° C.) of arc welds in mild steel raised the pulsating tension fatigue limit by 2800 psi,

which was also the effect of normalizing ($^1/_2$ hr. at 920° C., air cool).

For mild steel the best rate of heating is 140 to 250° F. per hour (80 to 140° C. per hour), the preferable rate being 200° F. per hour (110° C.). The furnace should not be above 300° F. (150° C.) when the welded material is placed in it, and the material should not be removed until the furnace has cooled below 300° F. (150° C.). Other temperatures may be best for other steels.

Stress Relief by Overstrain

Stress relief by overstrain employs a uniform stress to relieve local residual stresses. The regions most highly stressed internally in the same direction as the applied external stress may undergo local inelastic action if the yield point of the material under the particular distribution of residual stresses is exceeded. On release of the external stress uniform elastic recovery occurs over the entire cross section, the residual stresses being less than before overstrain to the extent of the inelastic action. Overstressing evens out the residual stresses, if there is adequate ductility or absence of high multi-axial stresses, but impact overstressing may cause cracks.

Experiments show that overstrain, apart from other possible effects, is effective in reducing residual stresses in welded joints. A number of repetitions of the external load is particularly effective in reducing the peaks of the residual stresses. Some investigations show reductions of residual stresses to 7000 psi by sufficiently large applied external load to cause permanent deflection.

The Time Effect in Stress Relief at Room Temperatures

The gradual release of residual stresses in welded steel at room temperature under the influence of time, or of time and chance shocks and vibrations has never been amply demonstrated. It is certain that dimensional changes at room temperature occur over long periods of time in quench-hardened steels. It is also known that stress relief by annealing occurs in welded steel at temperatures below that required to change room-temperature tensile strength and hardness. The tensile elastic limit of a welded specimen is lowered to an extent approximately proportional to the maximum residual tensile stress. The yield point and tensile strength are scarcely affected by residual stress.

Effect of Residual Stress on Buckling

Residual stresses caused by welding having been shown to have an effect on the elastic behavior of steel, it is conceivable that residual stresses may have a considerable effect on the strength of welded structures, such as columns, in which elastic stability is the determining factor. However, experiments have shown that, for the columns tested, residual stresses do not have pronounced effects on buckling.

Effect of Residual Stress on Fatigue

Residual tensile stresses (or compressive stresses) due to welding beams are of secondary importance to fatigue strength.

Cracks in Steel

Cracks may occur due to residual stresses at two stages during the production of a welded joint in mild steel: (1) at or just below the solidus; (2) in the blue brittle range (220–300° C.); or upon application of load. Ductile filler metal and avoidance of stress concentration are helpful in overcoming cracks. Preheating and reheating are useful means of avoiding cracks. Peening is of doubtful value. Cold peening requires a ductile weld metal. Hot peening of base metal next to the weld is useful in avoiding cracks in welds made under rigid constraint. Hot peening also closes pores which may serve as nuclei for cracks.

In air hardening steels cracks generally occur in base metal adjacent to the weld, and are caused by a combination of fabricating and welding stresses. The tendency of steels to crack during welding increases with the sensitivity of the steel to overheating.

In relatively thin sections, such as aircraft sheet and tubing, sensitivity to cracking usually increases with decrease in plate thickness and with excessive overheating during welding, as well as with increase in sulphur content at constant carbon content. In thicker sections of any steel cracking may increase or decrease with increase in thickness. It is believed in some quarters that there is greater difficulty in avoiding cracks in welded plain carbon steel when the manganese content of the weld is between 0.3 and 0.6% than with higher or lower manganese content, but no definite experimental work has been published in confirmation.

Tests have been evolved to determine the sensitivity of steel to cracking during welding. The tests are suited mainly to sheet steel for which the tests consist either of welding under conditions of pre-determined restraint, or, less preferable, of welding under free conditions or under conditions in which the degree of restraint is not definitely known and may vary for different steels.

Welds Other Than Mild Steels

Consistent with its higher elastic strength at low and elevated temperatures, low-alloy structural steel has higher residual stresses than mild steel for a given type of joint and welding procedure, and the same mass effect. Ordinarily, alloy steel structures are lighter than the equivalent structure in unalloyed steel.

Theoretical Aspects

The summary of experimental results leaves the impression that there is much room for theoretical developments connected with shrinkage stresses in welds. The two theories now advanced are the Elastic and the Inelastic. The Elastic theory takes no account of permanent deformations in welding which are known to occur practically always and which exert a great effect on residual stresses. The Inelastic theory makes use of these permanent deformations in connection with changes in temperature and physical properties during welding to determine the residual elastic stresses in the completed weld. The Inelastic theory of which great hopes may be held, is in a rudimentary stage of development at present. The theoretical basis of the release of residual welding stress by overstrain is likewise not satisfactorily developed.

Research Problems

Special attention is called to a list of research problems at the end of the report.

Universities and others interested are invited to cooperate in the solution of some of these problems.

Shrinkage Stresses in Welding

Introduction

THE rupture of parts, such as rigid cast-iron frameworks, by locally changing the temperature to a moderate extent is well known. Failure is caused by the so-called thermal stresses, which are a result of local temperature strains. Parts to be fusion welded are practically always subjected to large and local temperature variations which create local strains. The local strains are created at the very first application of heat and vary throughout the entire process of welding and subsequent cooling. The strains may be due to the effects of temperature alone, or to changes from liquid to solid; from austenite to pearlite; from non-magnetic state to magnetic, etc. Except under unusual unrestrained conditions these local strains give rise to local stresses, the subject of the present Review, which vary throughout the period of welding and cooling, but which, in general, by no means vary in direct proportion to the local distortions. Although the stresses exist and vary throughout the process of welding, it is only the stress in the finished weld after cooling to normal temperature that has been experimentally investigated. The methods and results of these investigations on so-called "residual stresses" are described and summarized in the first sections of this Review starting with the elementary butt joint. A concluding section discusses theories on the variation of stress in a welded part throughout the process of welding, upon which question there is, unfortunately, little experimental information. A number of limited general reviews of the question of residual stresses in welds have already appeared.[1]

Butt Joints

Oxyacetylene Welds Without Restraint

Investigations of the residual stress in oxyacetylene welded plates free to move during welding have been made by Bollenrath,[2] Mies,[3] Bierett and Grüning[4] and Siebel and Pfender.[5] Bollenrath used the Mathar Method, and a modification thereof that is described along with the mild steel plates he employed, in the section on "Arc Welds Without Restraint." His results are shown in Table 1, the maximum residual stress being found in the weld except for the fourth weld listed in the Table. In this weld a maximum residual tensile stress of 76,000 psi was found a little beyond the weld. Right-hand welding was used in all the experiments with the modified Mathar Method. Step-backs were in six steps, 4 inches each. The filler rod had tensile strength = 51,500 psi, yield point = 31,500 to 37,000 psi.

The difference between the maximum residual stresses parallel and perpendicular to the weld in Table 1 determined by the original Mathar Method is 36,000 to 39,200 psi. Mohr circles (plots of normal stresses and derived shear stresses) showed that the yield point in shear (17,000 to 18,500 psi) had nowhere been exceeded. Plotting the results of the unmodified Mathar Method as a function of plate thickness it was found that the stress perpendicular to the weld should be zero at a thickness of 0.43 inch, the principal stress parallel to the weld at this thickness being equal to the yield point. To account for the 50% increase in yield point observed for the welds 0.59 inch thick, it was felt probable that the third principal stress must be present.

The surprising difference between the results obtained by Bollenrath using the unmodified and modified Mathar Methods is not explained. The results obtained by the modified Mathar Method do not appear to show any consistent difference between step back and continuous welding, or between welds of the same size requiring different lengths of time to execute.

Table 1—Residual Stresses in Free Oxyacetylene Welded Butt Joints. Bollenrath[2]

Plate Thickness, Inch	Type of Joint	Welding Procedure	Maximum Residual Stress, psi (− = compression) Parallel to Weld	Perpendicular to Weld
		Original Mathar Method		
0.59	V	Tacked by arc, right-hand welding	72,000	36,000
0.59	V	Tacked by arc, right-hand welding, peened at red heat (no details)	66,500	29,000
0.59	V	Held open by wedge, right-hand welded to middle, finished by left-hand welding	45,500	9,300
0.39	V	Tacked by arc, right-hand welding	34,500	− 3,500
0.39	V	Tacked by arc, right-hand welding (apparently same procedure as preceding)	42,000	5,000
0.20	V	Tacked by arc, right-hand welding	29,200	−10,000
		Modified Mathar Method		
0.59	V	Step back, rod diameter = 0.24 inch, time of welding = 58 min.	25,600	30,000
0.98	X	Step back, rod diameter = 0.50 inch, time of welding = 124 min.	40,000	37,000
0.59	V	Continuous, rod diameter = 0.50 inch, time of welding = 48 min.	40,000	28,500
0.59	V	Step back, rod diameter = 0.50 inch, time of welding = 57 min.	57,000	71,000
0.79	X	Continuous, rod diameter = 0.50 inch, time of welding = 53 min.	41,500	31,500
0.98	X	Step back, rod diameter = 0.50 inch, time of welding = 125 min.	25,600	34,000

The method adopted by Mies[3] for measuring residual stresses in free, gas-welded plates was the same as that described in the section on Free Arc Welds. The plates to be welded were machined from full-annealed, 0.2% C steel plates (yield point 35,000 psi, tensile strength 60,000 psi, elongation 24.1% (gage length not stated), reduction of area, 37.3%) originally 0.79 inch thick. About 0.16 inch was machined from each side and the surfaces ground to avoid initial surface stress. The plates were 24 (welded edge) x 28 inches. The plates were oxyacetylene welded with a 0.16-inch rod in 54 minutes (4 rest periods of about 3 minutes each). The weld was 60° V, not tacked, 0.12-inch shoulder, with an allowance of 0°–40′ for angular distortion.

The 0.003% elastic limit (28,500 psi) was everywhere exceeded at some time during welding and cooling, but the residual stresses, Fig. 1, over most of the plate were small. The residual stresses in the weld itself were not measured. Equilibrium calculations indicated that stresses perpendicular to the weld exceeded 43,000 psi

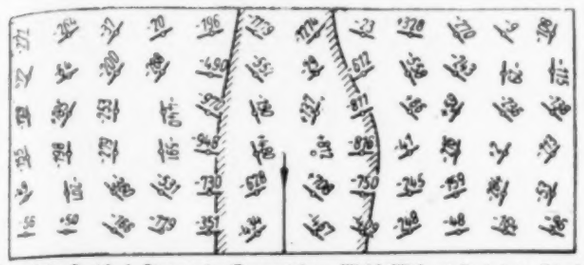


Fig. 1(*a*)—Residual Stresses in Oxyacetylene Weld Without Restraint. Principal Stress S_1 and Its Direction at All Points. Multiply Values by 14.2 to Obtain Pounds per Square Inch. Arrow Indicates Direction of Welding. Mies[3]

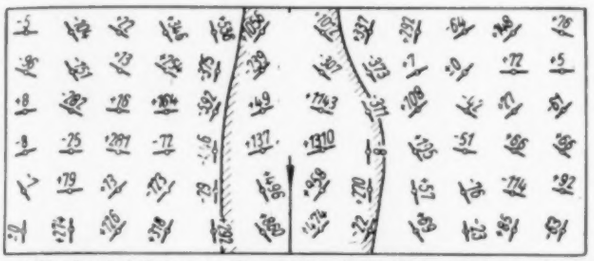


Fig. 1(*b*)—Residual Stresses in Oxyacetylene Weld Without Restraint. Principal Stress S_2 and Its Direction at All Points. Multiply Values by 14.2 to Obtain Pounds per Square Inch. Arrow Indicates Direction of Welding. Mies[3]

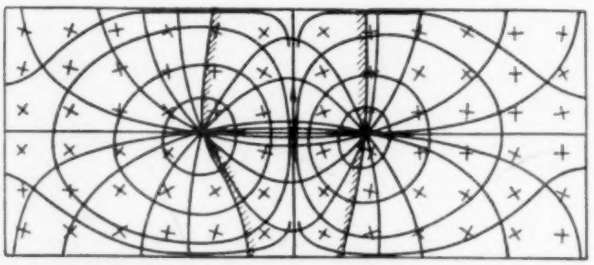


Fig. 1(*c*)—Residual Stresses in Oxyacetylene Weld Without Restraint. Lines of Principal Stress. Shaded Area Is Heat-Affected Zone. Arrow Indicates Direction of Welding. Mies[3]

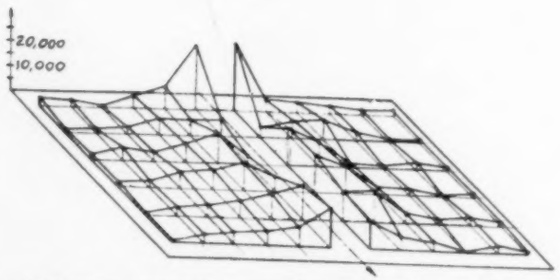


Fig. 1(*d*)—Residual Stresses in Oxyacetylene Weld Without Restraint. Reduce Stress = S_T

$S_T^2 = S_1^2 + S_2^2 - S_1S_2$

Average of values (psi) on both sides is plotted. Straight lines connect the plotted points. Arrow indicates direction of welding. Mies[3]

tension at the edges. In the remainder of the plate these stresses were compressive (−7000 psi). Compressive stresses parallel to the weld were 14,000 to 17,000 psi.

Using the method described in the section on Arc Welds Without Restraint, Siebel and Pfender[5] measured the residual stresses in a continuous 90° X oxyacetylene weld (mild steel filler rod) 24 inches long in two mild steel plates 8 inches wide, 1.18 inches thick (yield point 35,500 to 37,000 psi, tensile strength 58,000 to 60,000 psi). The distribution of residual stresses was unaccountably complex, the maximum values being −14,000 psi parallel to the weld, and −28,500 psi perpendicular to the weld. The starting end of the weld had a residual tensile stress of 28,500 psi. The other end had a compressive stress of equal magnitude.

The method and plates employed by Bierett and Grüning[4] were precisely the same as those described in the section on Arc Welds Without Restraint except that the oxyacetylene weld was made with a neutral flame in a continuous layer with a mild steel filler rod 0.47 inch diameter. The residual stress in the weld and perpendicular thereto varied from −28,500 psi at the ends to +10,000 psi at the middle. Unlike the free arc weld the gas welded plates had greater transverse residual stresses (−38,500 psi) at the edge of the plate at which the weld was finished at a distance of about 1 inch from the weld than in the weld itself. The residual stresses in the weld and parallel thereto were maximum (+17,000 psi) at the center. The variation of this longitudinal stress along the center line of the plates was similar to the arc welds in that at a distance of about 4 inches the stresses changed from tension to compression (−13,000 psi maximum). But before decreasing with distance from the center of the weld, the stress parallel to the weld increased until at a distance of about an inch from the weld there was a maximum tension of 23,000 psi. This type of stress distribution is probably more nearly correct than that shown by Mathar[6] for oxyacetylene welds. The latter, who supplies no details, did not observe the decrease in stress at the weld itself.

Oxyacetylene Welds with Restraint

The residual stresses in oxyacetylene welds made under conditions of more or less restrained shrinkage have been studied by Bühler and Lohmann,[7] using the Sachs method. The formulas involved are

$$\text{Tangential Stress} = \frac{E}{1-u^2}\left[(A_0 - A)\frac{dT}{dA} - \frac{A + A_0}{2A}\right]T$$

$$\text{Radial Stress} = \frac{E}{1-u^2}\left[\frac{A_0 - A}{2A}\right]T$$

where E = Young's Modulus
u = Poisson's Ratio
A_0 = cross section of undrilled disk
A = cross section of drilled disk
T = change in thickness caused by drilling
$\frac{dT}{dA}$ = rate of change of T with A at the stage of drilling under consideration.

Disks of soft steel (St 34) and low-alloy structural steel (St 52, see section on Arc Welds with Restraint for chemical composition and physical properties) 9.8 or 19.6 inches outside diameter had circular patches 5.9 inches diameter welded in the center, as shown in Fig. 2. Disk and patch were 0.79 inch thick. For right-hand welding the joint angle was 70°, for left-hand welding 90°. The filler rods were 0.16 inch diameter; their analyses are given in Table 2.

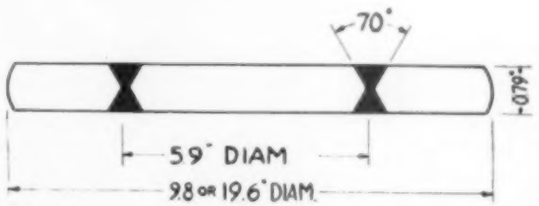


Fig. 2—Disk Specimen Used by Bühler and Lohmann.[7] Root Spacing = 0.08 Inch

Table 2—Composition of Filler Rods Used by Bühler and Lohmann[7]

Rod	C	Si	Mn	P	S	Cr	Ni	Cu
For Soft Steel	0.22	[0.24]	0.85	0.026	0.027	..	..	..
For Low-Alloy Steel	0.17	0.45	0.90	0.024	0.025	0.06	0.72	0.38

After being welded the disks were drilled out successively to 2, 3, $3^3/_4$, $4^1/_2$, $5^1/_4$, 6, $6^3/_4$, $7^3/_4$, $8^1/_2$ and $8^3/_4$ inches inside diameter. After each step the change in outside diameter of the disk was measured along two diameters at 90°, the corresponding stresses being computed by means of Sachs' formulas.

The results are shown in Table 3. For the sizes and type of specimens that were tested, right-hand welding and mild steel gave less residual stress than left-hand welding and low-alloy steel, but these may not be general rules. The magnitude of residual stress depends to a preponderating extent on the relative volumes of elastic and inelastic material in the specimens during welding. The greater the restraint exerted by the elastic material, the higher is the residual stress. It must be pointed out that the welds tested by Bühler and Lohmann were low-grade, as shown by X-ray, although no specimen showing cracks in the X-ray was used for stress measurements.

Table 3—Residual Stress in Oxyacetylene Welded "Patches." Bühler and Lohmann[7]

Steel	Outside Diameter of Disk, Inch	Method of Welding	Maximum Tensile Stress in Tangential Direction, psi	Tangential Compressive Stress at Outer Edge, psi	Tensile Stress in Weld, psi Tangential	Radial
Mild St 34	9.8	Right-Hand	10,000	10,000	4,300	1,400
" " "	9.8	Left-Hand	1,400	2,800	0	1,400
" " "	19.6	Right-Hand	32,800	22,800	20,000	20,000
" " "	19.6	Left-Hand	34,200	24,200	8,600	21,400
Low-Alloy	9.8	Right-Hand	10,000	10,000	1,400	1,400
" "	9.8	Left-Hand	5,700	11,400	0	2,800
" "	19.6	Right-Hand	42,700	41,300	27,000	27,000
" "	19.6	Left-Hand	34,200	38,500	28,500	22,800

The residual stresses in oxyacetylene welded butt joints in the form of a circular patch have also been determined by Bierett and Grüning,[4] and Bollenrath.[2] Bierett and Grüning had an expert welder prepare three specimens, Table 4, each consisting of a patch 8 inches diameter left-hand welded (0.08-inch root spacing) from one side in the center of a plate of the same steel 80 inches square, the plates being in a vertical position during welding. The patches were given a convexity of 0.32 inch by hammering to relieve somewhat the rigidity of the welding condition. Plates and patches were stress relieved before welding. The steel contained 0.10–0.12% C and had a yield point of 31,500 to 35,500 psi, tensile strength 56,000 to 58,500 psi, elongation 24 to 26% (gage length not stated). The welds were made in two sections of 180° each starting from the same point. Mild steel rods 0.16 and 0.20 inch diameter were employed. The reheating was by torch to a red heat.

Extensometer measurements on both sides of the specimens were made after welding and after cutting, along 2 to 8 radii at distances of 0, $2^3/_8$, 4, $5^1/_2$, $7^1/_8$, $9^3/_4$, $15^3/_4$, $23^1/_2$ and $35^1/_2$ inches from the center. The distribution of the average stresses along a radius is shown in Fig. 3. Close agreement of the distribution with Bühler and Lohmann's is not to be expected. The stress measured on the root side was often the reverse of that measured on the face side. For example, in the second specimen in Table 4 the maximum tangential tensile stress of 35,900 psi occurring on the root side was accompanied by a tangential compressive stress at the same radius of 14,200 psi, whereas a stress of +34,300 psi on the root side at another radius was accompanied by +28,500 psi on the face side. The difference is ascribed to varying warpage of the plate, caused principally by the tangential stress.

The results show that the large heat input in the first specimen did not cause large residual stresses. Only where a patch is near a highly stressed region, such as a boiler manhole, should an effort be made to reduce the welding time. Although the average residual stress in the locally annealed specimen (annealed by moving the

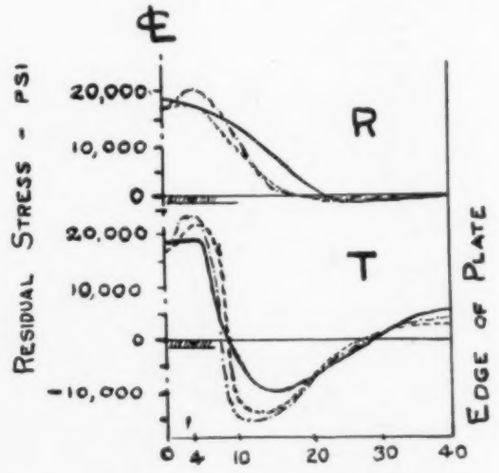


Fig. 3—Average Residual Stresses Along a Radius of Patch Welded Disks. (Oxyacetylene.)

——— 0.59-inch plate, peened and reheated
– – – – – 0.39-inch plate, not peened and not reheated
—·—·— 0.39-inch plate, peened and reheated
T = tangential stress
R = radial stress
Bierett and Grüning[4]

Table 4—Residual Stresses in Oxyacetylene Welded Patches. Bierett & Grüning

Plate Thickness, Inch	Angle of V Degrees	Treatment	Weight of Deposited Metal, Lb.	Gas Consumption, Cu. Ft.	Time of Welding, Min.	Average Residual Stress, psi Radial Tension	Radial Compression	Tangential Tension	Tangential Compression	Maximum Residual Stress, Radial	Maximum Residual Stress, Tangential
0.59	90	Peened and reheated	2.7	124	109	19,300	1400	20,000	10,000	+34,200	+32,800
0.39	70	Not peened and not reheated	0.94	37	37	17,800	1400	22,800	14,200	+49,000	+51,000
0.39	70	Peened and reheated	0.90	42	46	21,400	1400	25,000	15,700	+31,600	+34,400

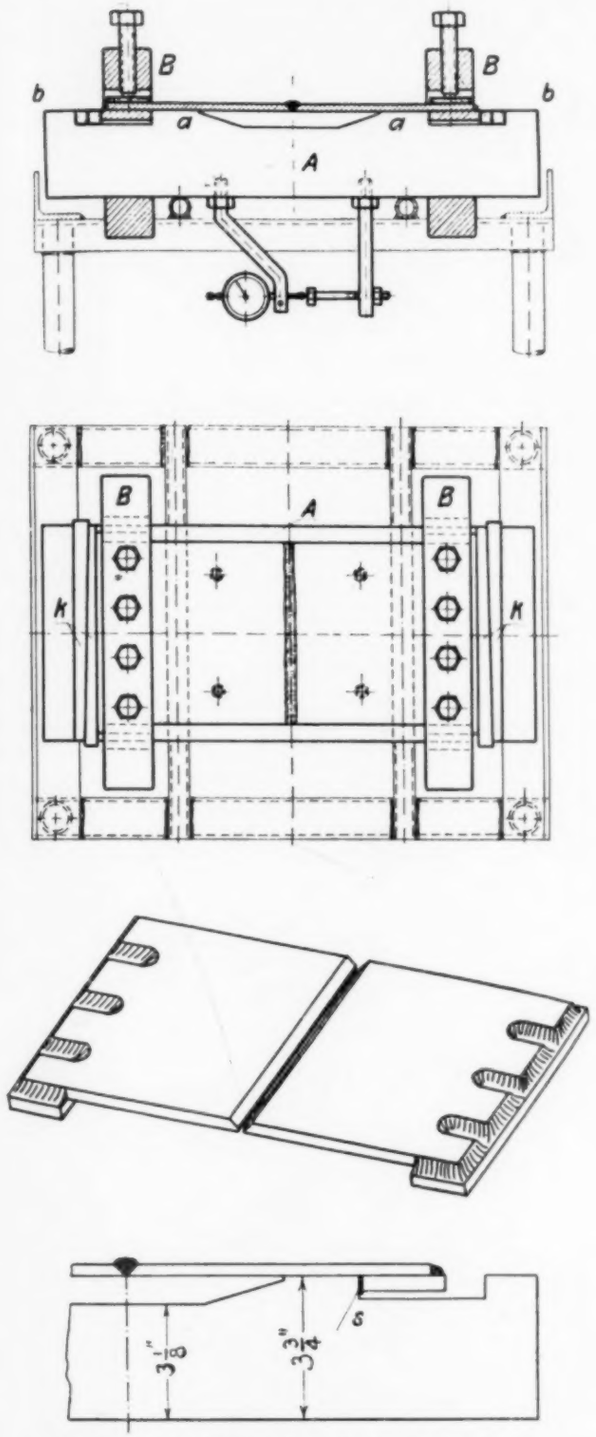


Fig. 4—Method of Measuring Stresses Used by von Roessler[6]
A—Massive block calibrated for stress measurement
B—Clamps with bolts to hold specimen to block *A* during welding
K—Pairs of wedges to secure contact between specimen and block
S—Shims

torch over the entire weld until it reached a dull red, followed by air cooling) was a little higher than in the unannealed specimen, the maximum stress was only two-thirds as large and the difference between the root and face sides was equalized to a great extent.

The oxyacetylene welded patch studied by Bollenrath,[2] who used the Mathar Method was 6 inches square and was 60° V welded in an annealed mild steel plate 24 inches square, both being 0.59 inch thick. The entire weld was not studied but a maximum tensile stress of 48,500 psi was found.

Bierett and Grüning[4] also investigated residual stresses in the specimen restrained by fillet welding described in the section on Arc Welds with Restraint. The welding conditions and method of measurement are described in the section on Oxyacetylene Welds Without Restraint. The residual stresses in the joint after it was cut loose from restraint were practically the same as those found in oxyacetylene welds without restraint. As a matter of fact, the gripped specimen had 10 to 20% less residual stress perpendicular to the weld than the free gas weld. Before being cut, however, there was a tensile "reaction" stress perpendicular to the weld of 21,500 psi to 28,500 psi over the entire joint. Before being relieved from restraint, therefore, the specimen had a maximum tensile stress of 33,000 psi perpendicular to the weld. The residual stresses parallel to the weld were practically the same in the gripped as in the free specimens.

The device used by von Roessler[6] for measuring residual stress perpendicular to the weld in single-V oxyacetylene welded butt joints is shown in Fig. 4. The mild steel plates to be welded ($8^1/_2$ x 6 x 0.32 inch welded on 6-inch edge) were fillet and slot welded to plates *K*, which were wedged against the calibrated compression bar *M*. Residual stresses parallel to the weld were measured by observing the change in width of the plate by means of a dial gage as the weld was gradually cut out with a straddle mill, using plenty of coolant. The angle of V was 90° for left-hand welding; 60° for right-hand.

The results are shown in Table 5. Allowing play at the wedges naturally decreased the reaction stresses, which were the stresses measured by the compression bar. Reverse welding caused +13,200 psi parallel to the weld.

Table 5—Residual Stresses in Oxyacetylene Welds with Restraint. von Roessler[6]

Method of Welding	Tensile Stress Parallel to Weld, psi	Tensile Stress Perpendicular to Weld, psi: Rigidly Wedged	Tensile Stress Perpendicular to Weld, psi: 0.04-Inch Play at Both Wedges
Left-hand	15,100	38,400	24,200
Right-hand	19,200	36,400	22,800

A right-hand oxyacetylene weld heated for two minutes with two torches had a stress of only +11,400 psi parallel to the weld.

The residual stresses created in mild steel plates by welding cracks were investigated by Reinhard[9] who employed the subdivision method, orthogonal network with $1^5/_8$-inch spacing, and a microscope accurate to ±7100 psi. The "crack" was a 90° V slot 12 inches long, 0.08-inch root spacing, in the center of a plate 60 inches long. The plate was 48 inches wide and 0.55 inch thick. No details of welding and materials are given. Stresses from 7000 to over 27,000 psi compression were found in the edge strips cut from such specimens. After the first cut the specimen cracked. No further change in the network was detected upon subsequent cutting. Relief of elastic residual stress by cracking was therefore considered to be complete.

Mies[3] was also unsuccessful in an attempt to determine residual stresses in oxyacetylene welded butt joints with restraint. He welded a specimen identical in dimension with that described in the section on Arc Welds with Restraint. The weld was made in a single pass occupying 14 minutes by means of a "torpedo" torch, 21-lb.

Table 6—Residual Stresses in Free Arc Welded V Butt Joints, Mathar Method. Bollenrath[2]

Plate Thickness, Inch	Diameter of Electrode, Inch	Type of Electrode	Sequence of Welding	Maximum Residual Stress, psi (− = compression) Parallel to Weld	Perpendicular to Weld	Difference psi
0.59	0.16	Bare	4 layers, 140 amp. 2 hr. cool after second layer; water cooling after last layer	107,000	.....	.....
0.59	0.16 and 0.20	Covered	2 layers (0.16 in. 120 amp.) 3 " (0.20 " 140 ")	135,000	33,000	102,000
0.59	0.20	"	140 amp. back step welding (steps 4 3/4 in. long) wedged apart 0.28 in. at start	134,000	84,000	50,000
0.39	0.16 and 0.20	"	2 layers (0.16 in. 120 amp.) 1 layer (0.20 in. 140 amp.)	104,000	11,400	92,600
0.20	0.16	"	1 layer, 80 amp.	70,500	9,500	61,000
0.12	0.08	"	1 layer, 35 amp., not Veed.	47,200	−21,400	68,600

oxygen pressure. Three hours after welding the weld was cool enough to bear touching with the hand and the side frames had not distorted. During further cooling, however, the side frames buckled away from the weld. The distance of buckling in a length of 62 inches was 0.65 inch in the frame next to the start of the weld, and 1.18 inches in the frame next to the end of the weld. An approximate calculation, assuming the side frames to act as fixed-ended columns, showed that the compressive stress in the side frames probably exceeded 29,000 psi near the end of the cooling process. Since the buckling of the side frames destroyed the condition of semi-rigid restraint provided by the elastic action of the unbuckled frames, no measurement of residual stress in the welded joint was made.

Arc Welds Without Restraint

A comprehensive investigation of butt welds in free plates has been made by Bollenrath,[2] who employed the Mathar Method, Table 6 (see Appendix on Methods of Measurement). The 1/2 inch Mathar holes were made in and adjacent to the weld metal. The plate was structural steel (12 x 24 inches, welded on 24-inch edge) which had been heated 8 hr. at 850° C. and cooled 24 hr. in furnace. The welds were 60° V, all made as rapidly as possible by the same welder. The physical properties of the plate and electrodes are shown in Table 7.

Table 7—Physical Properties of Base Metal and Deposited Metal Used by Bollenrath[2]

	Plate	Covered Electrodes	Bare Electrodes
Tensile strength, psi	51,500	71,000 to 82,500	56,000 to 59,500
Yield point, psi	35,000 to 37,000	56,000 to 71,000	43,000 to 45,500
Brinell hardness	.....	167	185
Elongation % (gage length not stated)	.....	5 to 10.2	6

Bollenrath assumed that the measured stresses were principal stresses and plotted them according to Mohr's Method. The Mohr circles for those cases in which the difference between the principal stresses was very large obviously could not be fitted inside a Mohr envelope. The differences were 30% higher than predicted by the Mohr theory for 0.39-inch plate and 44% higher for 0.59–inch plate. In order to explain the extraordinarily high elastic stresses in the weld, Bollenrath assumed that the multi-axial stress condition effected a rise in the yield point (as determined in the usual uni-axial tensile test) of as much as 90%. Since it was difficult to believe that bi-axial stress alone could be responsible for such a rise (maximum increases of 15% having been observed experimentally due to bi-axial stress), Bollenrath inferred that the rise was occasioned by tri-axial stress. This deduction is not absolutely unlikely, the yield point of metals under tri-axial (hydrostatic) compression having not yet been reached by pressures obtainable at present.

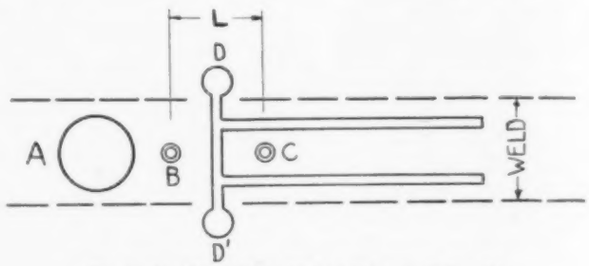


Fig. 5—Modified Mathar Method Used by Bollenrath[2]
A = Mathar hole 0.47 inch diameter
B and *C* = Mathar extensometer holes
D and *D'* = holes to equalize stress at ends of saw cut

Bollenrath attributed the high residual stresses to the great difference in yield point between the plate and covered electrode weld metal. The relatively low stress in the bare electrode weld may thus be accounted for. Bollenrath offered no detailed explanation for the great effect exerted by non-uniformity of elastic strength in the vicinity of the weld. On plotting the stress differences in the last column of Table 6 against plate thickness, Bollenrath found that the stress in the weld and perpendicular thereto should be zero for 0.24-inch plate. In thicker plate the third principal stress must be called into being to account for the remarkably large increase in yield point.

Bollenrath realized that the results of the Mathar Method are valid only if inelastic action not duplicated in the calibration specimen does not occur around the periphery of the hole. Mesmer[10] has shown by Fry etching that such action may occur to a pronounced extent in welds. If inelastic action occurs, the observed deformation and stresses calculated therefrom are larger or smaller than the elastic residual stresses, which are the only ones that the Mathar Method is expected to detect.

With the view of eliminating the suspected difficulty, Bollenrath modified the Mathar Method, Fig. 5. Gage points *b* and *c*, 0.39 inch apart, are made and the Mathar hole *A* is drilled, *L* being measured before and after drilling. Then the saw cut between holes *d* and *d'* is made perpendicular to the gage line, *L* again being measured. The holes equalize the stress at the end of the saw cut. Then two cuts 0.20 inch wide and 0.79 inch long are made parallel to the weld, 1/8 to 3/16 inch being cut on one side then an equal amount on the other, to avoid too rapid decrease of stress. As different lengths of the strip are cut the changes in *L* are measured and converted by means of Young's Modulus, thus showing the residual stress at sections along the strip.

Despite uncertainties of the extent of inelastic action during cutting, the modified method agreed fairly well with the unmodified Mathar Method. The results of measurements on butt welds made with covered electrodes (no details) in boiler plate 24 inch long (welded edge) and 12 inch wide are shown in Table 8.

Table 8—Residual Stresses in Free Arc-Welded Butt Joints, Modified Mathar Method. Bollenrath[2]

Plate Thickness, Inch	Diameter of Electrode Inch	Sequence of Welding	Time of Welding, Min.	Maximum Residual Stress, psi, Tension Parallel to Weld	Perpendicular to Weld
0.59	0.16	Stepback, V, 3 layers, 160 amp.	65	28,500	43,000
0.98	0.16	Stepback, X, 2 layers (0.16 in. 160 amp.)	100	50,000	54,000
	0.24	Stepback, X, 2 layers (0.24 in. 230 amp.)			
0.59	0.20	Continuous, V, 4 layers, 170 amp.	55	60,000	31,400
0.98	0.20	Continuous, X, 4 layers, 190 amp.	75	85,500	25,700

The step-back welds were made in 6 steps, 4 inches each. The residual stresses parallel to step back welds are about $^1/_2$ those in continuous layer welds, the stresses perpendicular to the weld being 25 to 100% higher. The stresses in the last weld corresponded to a rise in yield point of 25%.

The only other investigators to measure the residual stresses in free, arc-welded butt joints are Siebel and Pfender,[5] Bierett,[4] Theisinger,[11] Åkeson,[12] Mie ,[8] Reinhard,[9] and Matting,[13] all of whom used the subdivision method. Siebel and Pfender[5] used 90° X welds 28 inches long in mild steel plates 8 inches wide, 1.18 inches thick having a yield point of 36,000 to 37,000 psi, tensile strength 59,000 to 60,000 psi. Bare electrodes 0.16 inch diameter, 230 amp. were deposited in a single layer on each side, the layers being continuous or step-back (5 steps). Deformations parallel and perpendicular to the weld as a result of subdivision of the entire plate into strips $1^9/_{16}$ inches wide were measured by means of a wedge extensometer accurate to 0.00002 inch in 0.79 inch length. Deformations were reduced to stress by means of Young's Modulus and Poisson's ratio. Over 1000 measurements were made on each plate.

The residual stresses in both welds were a maximum in the immediate vicinity of the weld. Residual stresses parallel to the weld were maximum (43,000 psi tension for both welds) at the middle of the weld and zero at the edges. Perpendicular to the weld the maximum stress also occurred at the center of the weld (21,400 psi compression for continuous, 28,500 psi compression for the step-back weld). At a distance from the weld, however, the residual stresses were lower in the step-back weld than in the continuous. Although the variations of residual stress over the plates followed no regular pattern, the stresses on both sides were not greatly different.

On the other hand, great differences between the stress perpendicular to the weld on both sides of a V weld in a plate 29 x 19 x 1.10 inch were observed. The weld was made with special electrodes, the root layer being hand welded, the remaining layers machine welded. The distribution of stresses perpendicular to the weld was complex, but the distribution of stresses parallel to the weld was similar to that of the continuous X weld. Stresses parallel to the weld of over 43,000 psi tension were measured.

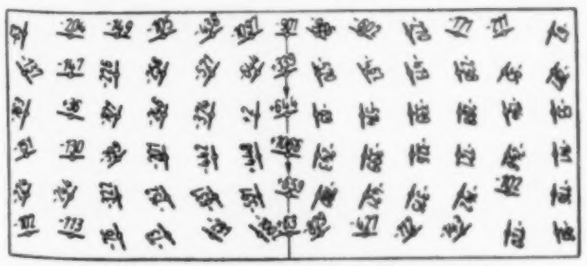


Fig. 6(a)—Residual Stresses in Arc Weld Without Restraint
Principal Stress S_1 and its direction at all points. Multiply values by 14.2 to obtain pound per square inch. Arrow indicates direction of welding. Mies[8]

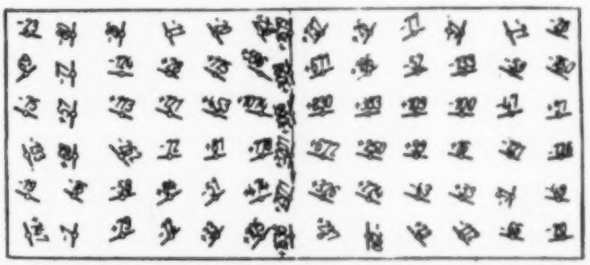


Fig. 6(b)—Residual Stresses in Arc Weld Without Restraint. Principal Stress S_2 and Its Direction at All Points. Multiply Values by 14.2 to Obtain Pounds per Square Inch. Arrow Indicates Direction of Welding. Mies[8]

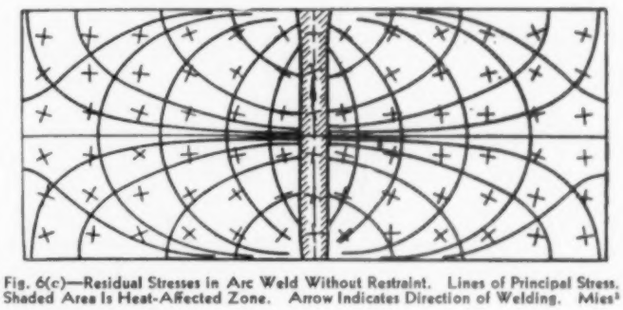


Fig. 6(c)—Residual Stresses in Arc Weld Without Restraint. Lines of Principal Stress. Shaded Area Is Heat-Affected Zone. Arrow Indicates Direction of Welding. Mies[8]

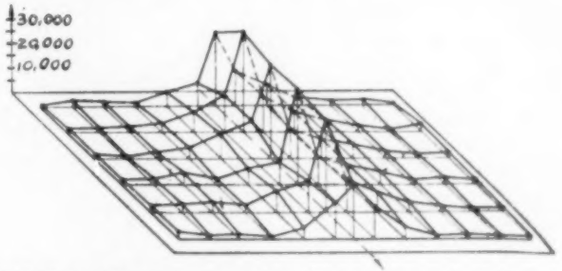


Fig. 6(d)—Residual Stresses in Arc Weld Without Restraint. Reduced Stress = S_r
$S_r^2 = S_1^2 + S_2^2 - S_1S_2$
Average of values in pounds per square inch on both sides is plotted. Straight lines connect the plotted points. Arrow indicates direction of welding. Mies[8]

Painstaking measurements (over 15,000 for each plate) on a single free, arc-welded specimen were made by Mies.[8] The plates were each 24 (welded edge) x 28 inches and were machined and ground to 0.47 inch thick from an original thickness of 0.71 inch. After being machined, the plates were heated 2 hr. at 600 to 650° C. and furnace cooled, the physical properties then being: yield point 32,200 psi, tensile strength 50,000 psi, elongation 30% (gage length not stated), reduction of area 61.3%. Thirty-six points (diamond scratches) were marked on each side of each plate, about 4 inches apart and six measurements at 60° to each other were made around each point before and after welding and after subdivision. Two microscopes were used (4-inch gage length) with a

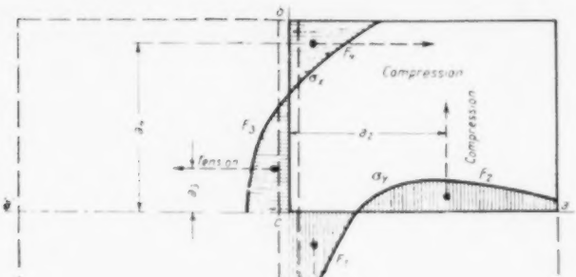


Fig. 7—Relation Between Residual Stresses in Arc-Welded Plates Without Restraint. Bierett[4]

screw micrometer in each objective accurate to 118 psi (greatest difference in observed stress at any point was 900 psi). The weld was 60° V, 0.12-inch shoulder, with an allowance of 0°–10′ for angular distortion. The weld was made in three layers (12 minutes for each layer, total welding time 40 minutes) using 0.16-inch bare electrodes, 17 volts, 145 to 150 amp. Plates were tacked over a distance of 0.39 inch at ends and middle.

After the weld had cooled each point was cut out, the stress due to the cutting not exceeding 1370 psi as determined by annealing at 650° C. Mies gives charts, Fig 6, showing the magnitude and direction of the two principal stresses, as well as lines of principal stress and reduced stresses $S_r^2 = S_1^2 + S_2^2 - S_1S_2$ (theory of constant energy of deformation). Permanent deformations occurred everywhere during welding, but over the greater part of the plates the residual stresses were small. The maximum residual stresses appeared in the weld averaging 36,000 psi tensile but exceeding 45,000 psi tension locally, in the direction parallel to the weld. Stresses in the weld perpendicular thereto were compression at the ends, but tensile in the middle. The maximum tensile stress perpendicular to the weld was 15,200 psi. The maximum residual stress in the plates outside the weld occurred perpendicular to the weld at the sides of the plates, and were tension except near the weld. The maximum was over 28,500 psi tension. The "reduced" stresses did not vary greatly along the 24-inch dimension of each plate, but increased from about 2000 psi at the end to 13,000 psi near the weld. In the weld the "reduced" stresses nearly coincided with the stresses parallel to the weld.

Bierett[4] also determined the residual stress in annealed mild steel plates 40 inches long, 12 inches wide, 0.47 inch thick, free to move during continuous welding on the 12-inch edge (3 layers 0.12-inch root-spacing 60° V) with bare mild steel electrodes, 120 amp. d.c. for first layer, 160 amp. d.c. for second and third layers. The subdivision method was used with an extensometer accurate to 0.00004 inch in 0.79 or 1.18-inch gage length. The residual stresses perpendicular to the weld and along its center line varied from −28,500 psi at the start of the weld to + 14,000 psi near the middle, and to −34,000 psi at the end. At a distance of about 3 inches on either side of the weld the compressive stress at the edge decreased to −7000 to −10,000 psi. The residual stresses in the weld parallel to the weld were, of course, zero at the edges rising uniformly to 30,000 psi in the middle of the weld. The characteristics of the stress distribution in arc-welded plates free to move are shown in Fig. 7. The effects of different welding procedures are shown in Fig. 8.

Residual stresses varying from 3000 to 20,000 psi tension were measured by Reinhard[9] in a 90° V weld, 0.08 inch spacing, 60 inches long in mild steel plates 0.55 inch thick, 24 inches wide, free to move during welding. Considerable bending was observed, but it was neglected in the stress computations A micrometer with a calculated accuracy of 8000 psi was used. Siebel and Pfender objected that the micrometer method is not sufficiently sensitive for worthwhile residual stress measurements. The welding procedure used by Reinhard was extraordinary. Short beads were deposited in the middle and at the ends, the gaps between them being filled subsequently.

Matting,[12] in summarizing his investigations on residual stress in unconstrained arc-welded butt Y joints, 7 inches long, states (without details) that the continuous bead method caused almost no residual stresses, but that any kind of step-back procedure produced residual tensile stresses of 25,000 to 43,000 psi varying sporadically along the seam. Also summarizing investigations on arc-welded butt joints, Mathar,[6] using his own method, stated that residual stresses parallel to the weld are usually near the yield point and are higher than those perpendicular to the weld. He found higher shrinkage stresses in continuous than in interrupted arc welds, but found no difference between bare and covered electrodes. He gives no details.

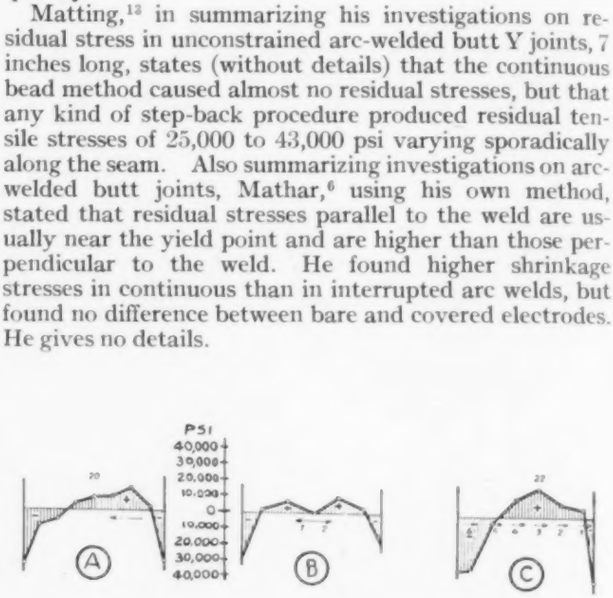


Fig. 8(a)—Residual Stress Perpendicular to the Weld in Arc Welds Without Restraint Using Different Sequences. Mild Steel, 1/2 Inch Thick, Cored Electrodes (Böhler-Elite) 3 Layers. Bierett[4]

Specimen	Length of Weld, Inches	Maximum Residual Stress, psi
A	12	−32,000
B	12	−27,500
C	12	−40,000
D	24	−27,500
E	24	37,500

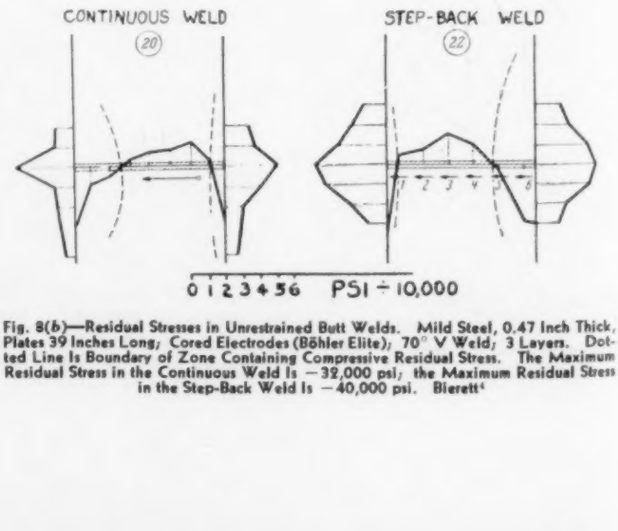


Fig. 8(b)—Residual Stresses in Unrestrained Butt Welds. Mild Steel, 0.47 Inch Thick, Plates 39 Inches Long; Cored Electrodes (Böhler Elite); 70° V Weld; 3 Layers. Dotted Line Is Boundary of Zone Containing Compressive Residual Stress. The Maximum Residual Stress in the Continuous Weld Is −32,000 psi; the Maximum Residual Stress in the Step-Back Weld Is −40,000 psi. Bierett[4]

Theisinger[11] studied a 90° V weld, 36 inches long in 12 x 7/16 inch, as-rolled structural steel tacked at ends and middle. The weld was made in 8 layers by hand using 3/16 inch coated electrode, 200 amp. 45 volts, each layer being deposited in the opposite direction to the preceding. Each layer was brushed and the completed weld was cooled in air. Extensometer measurements (2 1/2 inches gage length) were made on both sides before and after welding, and after cutting the welded plate into strips parallel and perpendicular to the weld. The maximum residual stress parallel to the weld was 42,000 psi tension in the weld itself. (Theisinger's plotted results appear to show that the maximum was 49,500 psi.) The yield point of the weld metal was 49,500 psi. The residual stress parallel to the weld rapidly fell to zero along lines parallel to, and about 1 1/2 inches from, the center line of the weld. Still farther from the weld the parallel stress became compression, up to −20,000 psi. The stresses perpendicular to the weld are not given.

Practically no difference was found by Åkeson[12] between the residual stresses in X and unbeveled butt welds 20 inches long in mild steel plates (53,000 psi minimum tensile strength) 6 inches wide, 0.39 inch thick. The X weld was made in 3 layers on each side with 0.16-inch coated electrodes (type ZA made by Aseas). The same type of electrode but 0.20 inch diameter was used in making the unbeveled weld, one layer on each side. After being welded the plates were cut into 20 strips parallel to the weld, as shown in Fig. 9. Deformations after cutting corresponded to a residual tensile stress perpendicular to the weld of 37,000 psi at the midpoint of the X weld and 38,500 psi at the midpoint of the unbeveled weld. In both cases the compressive stress perpendicular to the weld at the edge was 14,000 psi.

Paton and coworkers[14] used the subdivision method in determining the effect of plate thickness on residual stress parallel to 80° V metal arc welds in mild steel plate. The strain was measured on a gage length of 10 inches, which was nearly the entire length of the weld. Light-coated electrodes (180 amp.) were used. The welded specimen were cut into strips 0.47 to 0.55 inch wide parallel to the weld. The last four welds in Table 9 were started at the middle, welding proceeding from the middle toward the ends.

Table 9—Residual Stress Parallel to Free Arc Welds. Paton and Coworkers[14]

Plate Thickness, Inch	Diameter of Electrodes, Inch	Maximum Tensile Residual Stress Parallel to Weld, psi	D, inch
0.24	1 layer, 0.16 inch Back step, 3 steps	44,000 to 46,500	1.35 to 1.80
0.32	1 layer 0.20 inch	63,000	1.85
0.47	1 layer 0.20 inch	40,000	2.00
0.63	1 layer 0.16 inch 1 layer 0.20 inch	33,000	2.04
0.79	1 layer 0.16 inch 2 layers 0.20 inch	30,500	2.04

The results are given by Paton and coworkers in terms of unit strain which, however, they regarded as equivalent to stress. For this reason, the third column contains stress obtained by multiplying the unit strain given by Paton and coworkers by Young's Modulus. The stresses decrease as plate thickness increases and are maximum in the weld itself. The term "D" in the last column of Table 9, is the "half-breadth of the shrinkage zone;" in other words, D is the perpendicular distance from the middle of the weld to the section at which residual stress becomes zero. It should be noted that Paton is not in good agreement with other investigators.

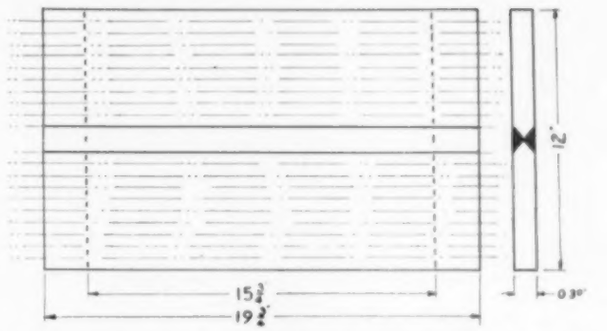


Fig. 9—Specimen Used by Åkeson[12] for Determining Residual Stresses in X and Unbeveled Butt Welds in Mild Steel. Three Layers Were Deposited on Each Side Using 0.16 Inch Covered Electrodes

–.–. = cuts made from each end nearly to middle of specimen
Gage length = 15 3/4 inches

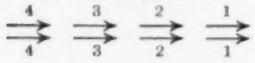
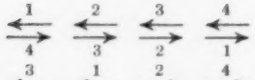
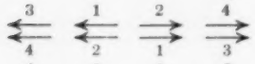
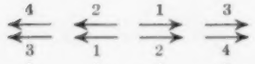
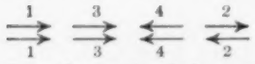
In addition to the investigations on residual stresses by means of the Mathar and subdivision methods, several attempts have been made to estimate residual stresses in unconstrained arc-welded butt joints by measuring (the direct method) the change between two gage points on a plate before and after welding. If all the metal within the gage length under consideration is known to have remained below the yield point at all times (and temperatures) during welding, the change in length of the gage length then provides a value of strain that may be used directly for calculations of residual stress. Since it is known that the maximum residual stresses in welds occur in just those regions near the weld which undergo inelastic action, the "Direct" method cannot be used to determine the maximum residual stresses, which are usually the significant data. Furthermore, it is often difficult to be certain that inelastic action has not occurred on a given gage length even though the temperature rise due to welding may have been negligible.

Nevertheless, Nikolaev[15] and Sarazin[16] have employed the "Direct" method to measure residual stresses in free arc-welded butt joints. Nikolaev used V welds (0.08 inch spacing) 18 inches long in mild steel plates 15 inches wide and 0.43 inch thick (except in one series in which the thickness was 0.71 inch). After each layer was deposited, strains were measured at four points along 3 lines parallel to the weld and 1 3/4, 4 1/2 and 9 1/4 inches, respectively, from the center line. Huggenberger tensometers (0.78 inch gage length) were used, the strain usually being measured perpendicular to the weld, sometimes parallel. The electrodes were 0.16 inch diameter.

The results obtained by Nikolaev are shown in Table 10, which summarizes a mass of data. The stress given in table is perpendicular to the weld unless otherwise stated. The maximum tensile stress usually occurred on the gage length nearest the weld. It should be remembered that the mid-point of the gage length closest to the weld was 1 3/4 inches from the center of the weld. In all probability therefore the maximum residual stress was not determined. The results show a vast difference between step back procedures and continuous layers and suggest that continuous layers in the same or opposite directions create the least residual stress perpendicular to the weld.

Extensometer measurements by Kusmak and Doronin[17] on plain carbon steel plates 0.39 and 0.67 inch thick 14 inches long X and V welded, respectively, showed that apparent residual tensile stresses perpendicular to the weld of 17,000 psi existed in the middle of the thinner plate and up to 40,000 psi in the thicker. The ends of

Table 10—Residual Stresses in Unconstrained V Butt Joints. Nikolaev[16]

Weld Sequence	Electrode	Current, Amp.	Max. Tensile Stress, psi	Max. Compressive Stress, psi	
	Light coated	160–180 d.c.	16,000	10,200	
"	Heavy coated	180–220 d.c.	15,000	6,000	
"	" "	110–170 d.c.	23,500	1,000	
	" "	160–190 d.c.	23,000	All tension	
"	" "	—— a.c.	14,500	" "	
6 layers in opposite directions	Light coated	160–170 d.c.	8,500	10,300	parallel to weld; 0.71
" " " " "	" "	" "	7,800	4,000	inch plate
4 layers same direction	Heavy coated	160–180 d.c.	26,500	21,000	
	" "	150–200 d.c.	11,200	27,000	
	" "	140–200 d.c.	21,500	above yield point	
	" "	140–200 d.c.	30,700	11,500	
	" "	130–200 d.c.	3,000	7,000	parallel to weld
	" "	160–190 d.c.	above yield point	above yield point	
3 continuous beads in same direction	" "	130–150 d.c.	1,500	4,500	

the welds were in compression perpendicular to the weld. It seems possible that the gage length underwent inelastic deformation in the latter case. The electrodes were covered, 0.24 and 0.35 inch diameter, 350 to 440 amp. The welds in the thinner plate were made in two layers; in the thicker plate the welds were made in four layers.

Sarazin,[16] who also employed the direct method, computed tensile stresses of up to 30,000 psi, one inch from a 60° V butt weld 10 inches long in 0.32-inch mild steel plate.

Arc Welds with Restraint (Straight Butt Joints)

Among the numerous investigations of residual stresses in plates arc welded under constraint, the investigation by Mies[3] must rank very near the top. He studied only one specimen but the results are among the most accurate we have. He estimated his overall accuracy to be ±1520 psi. The joint was prepared by machining as shown in Fig. 10. There were 44 gage points on both sides with three gage lines at 60° to each other intersecting at each point. Five gage lines 4 inches long were also drawn on each side of each rigid side frame. The total deformation of each side frame was also measured over a gage length of 45 inches by means of a comparator. The plate and method of measurement were described in the section on Arc Welds Without Restraint. The specimen was heated $2^1/_2$ hours at 600° C. and slowly cooled before welding. The physical properties were: yield point = 32,000 psi, tensile strength = 62,000 psi, elongation = 27.5% (gage length not stated). The welding was done at 20 volts, 160 amp. with 0.16-inch bare electrodes. Tacks $^3/_8$ inch long were made at the ends and middle. One third of the length of the joint was welded at a time; three layers were deposited in each section before proceeding to the next section. Each layer required about $2^1/_2$ minutes, the total welding time being 21 minutes. Measurements were made before and after welding and after cutting, only the two last measurements being used for stress calculations. The cut-out strips were not annealed, but it was shown that this omission involved an error of not more than 650 psi.

The magnitudes and directions of the principal stresses are shown in Fig. 11(*a*) The distribution of residual

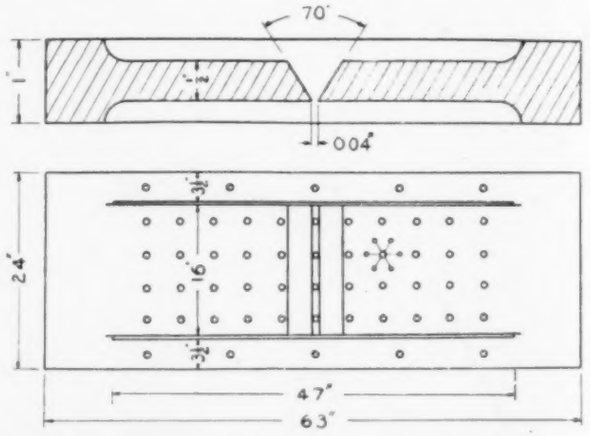


Fig. 10—Specimen for Arc Welding with Restraint. Dimensions in Inches. The Scale of Thickness Is 10 Times That of Lenth and Width. Slots 0.39 Inch Wide. Each Circle Is the Center of Three Intersecting Gage Lengths, 4 Inches Long on Both Sides. Mies.[3] 1935

normal stresses parallel and perpendicular to the weld is shown in Fig. 11(*b*), along with the reduced stresses, $S_r^2 = S_1 + S_2 - S_1S_2$. The results show that, for all practical purposes and with a few exceptions, the principal stresses are parallel and perpendicular to the weld. The principal stress perpendicular to the weld is less uniformly distributed than that parallel to the weld because the latter is equalized by the elastic load carried by the side frames. The maximum reduced stress occurred in the weld (25,300 psi) but was below the yield point as determined in the ordinary tensile test. The average "reduced" stress was: 21,600 psi in the weld,

16,800 psi in the heat-affected zone as revealed by temper colors,

9,700 psi in the remainder of the 0.47-inch plate.

Comparing the results of the arc welds made with and without restraint, Mies found that the stress distribution in the weld and its vicinity is not very different. In both cases the maximum tensile stress occurred in the weld and was parallel thereto; this stress was 23,000 psi (see Fig.

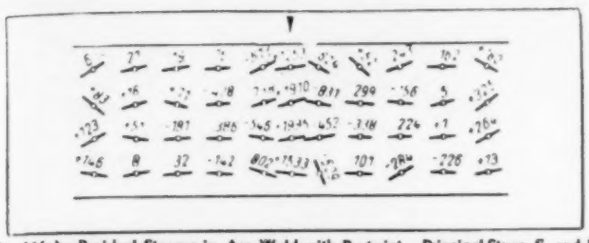


Fig. 11(*a*)—Residual Stresses in Arc Weld with Restraint. Principal Stress S_1 and Its Direction at All Points. Multiply Values by 14.2 to Obtain Pounds per Square Inch Arrow Indicates Direction of Welding. Mies[3]

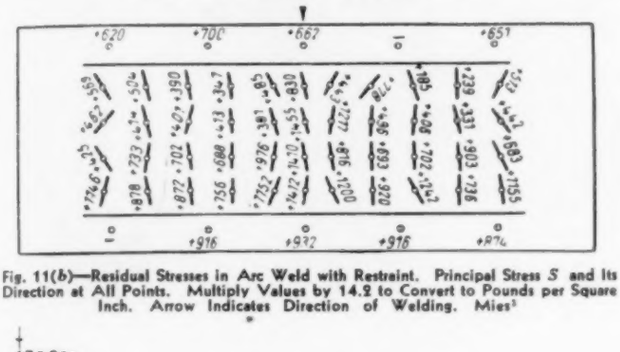


Fig. 11(*b*)—Residual Stresses in Arc Weld with Restraint. Principal Stress S and Its Direction at All Points. Multiply Values by 14.2 to Convert to Pounds per Square Inch. Arrow Indicates Direction of Welding. Mies[3]

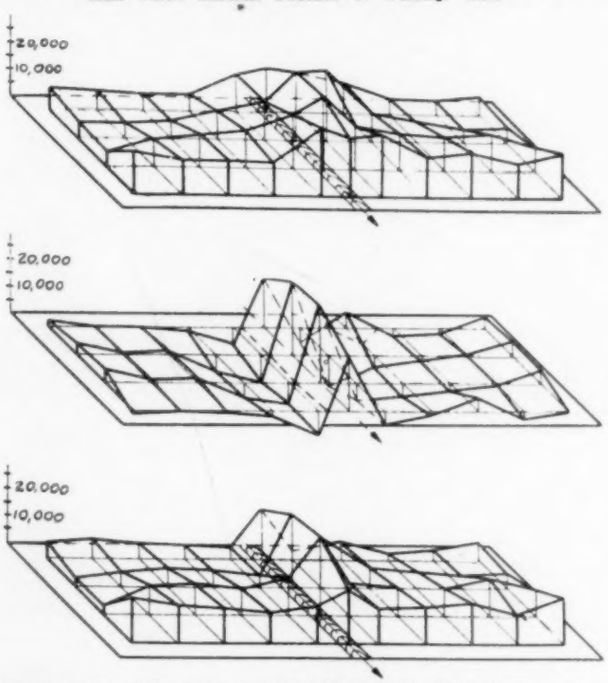


Fig. 11(*c*)—Residual Stresses in Arc Weld with Restraint. Reduced Stress $= S_T$
$S_T^2 = S_1^2 + S_2^2 - S_1 S_2$
Average values (psi) on both sides are plotted. The plotted points are joined by straight lines. Arrow indicates direction of welding. Mies[3]

Fig. 11(*d*)—Residual Stresses in Arc Weld with Restraint. Residual Stresses (psi) Parallel to Weld. Arrow Indicates Direction of Welding. Mies[3]

Fig. 11(*e*)—Residual Stresses in Arc Weld with Restraint. Residual Stress Plus Reaction Stress (psi) Perpendicular to Weld. Arrow Indicates Direction of Welding Mies[3]

11(*e*) showing normal stress parallel to weld) in the constrained weld, which is smaller than that found in the unconstrained. On the other hand, the average tensile stress in the constrained weld and perpendicular thereto was 18,400 psi, which is a little larger than the corresponding stress in the free plates. Mies arrived at the conclusion that the residual stress distribution in the constrained weld was more favorable than in the free—a surprising conclusion, but justified by the results.

The compressive stresses in the side frames are shown in Fig. 12. The fact that the average compression was 50% greater in the frame adjacent to the end of the weld

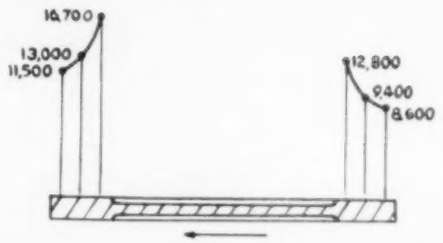


Fig. 12—Compressive Stresses (psi) After Welding in Side Frames of Arc Weld Made Under Restraint. Arrow Indicates Direction of Welding. Mies[3] 1935

than in the other frame is explained by the sequence of welding. When the last section of the weld was shrinking, the other two sections had cooled sufficiently to support some of the compression associated with shrinkage perpendicular to the weld. Consequently the higher principal tensile stresses found at the end of the weld correspond with the higher compressive stresses in the adjacent frame. In connection with the frames it should be observed that Mies does not appear to have protected them from the heat of the arc, nor does he mention specifically whether there was any difference between readings on both sides of the specimen.

A specimen somewhat similar to Mies' was used by Lance Martin,[18] Fig. 13. After being machined to size the specimen was annealed at 900° C. The three-layer arc weld was machine-made at a rate of 4.5 inches per minute with blue asbestos covered mild steel electrodes (Quasi-Arc) 100 amp. for 10 gage (2 layers); 125 amp. for 8 gage (150 cycles single phase). The layers were deposited in quick succession. The side frames were protected from the direct heart of the arc. The plate contained 0.18% C and had a yield point (unannealed) of 40,000 psi, tensile strength 63,000 to 72,000 psi, elongation 25% in 8 inches. Measurements on the gage lengths shown in Fig. 13 were made before and after welding and after cutting. They were made by means of a comparator, calibrated by means of an interferometer. Restraint was released by making two simultaneous hacksaw cuts from the outer edge to the slots. Strain was converted to stress by means of Young's Modulus (30×10^6 psi) and Poisson's ratio (0.28). There was an appreciable difference between measurements on the two faces of the specimen, but the differences were small and the average of both faces was used in calculations.

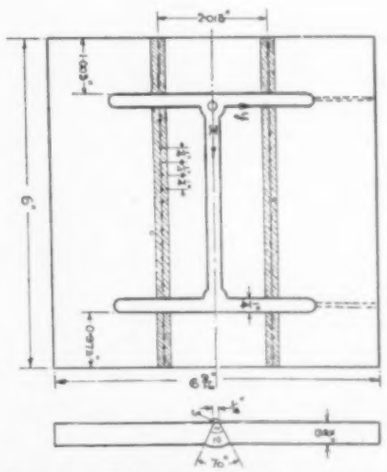


Fig. 13—Multiple Layer Arc-Welded Specimen Used by Lance Martin.[18] The Surface Was Polished; Gage Marks Are Shown Shaded. Simultaneous Saw Cuts Are Indicated by Double Dotted Lines

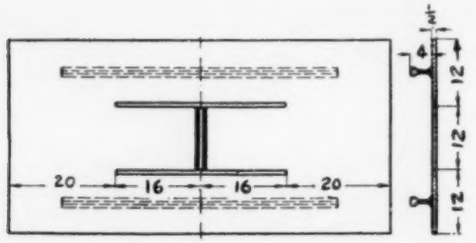


Fig. 14(a)—Type No. 1 Specimen Used by Bierett.[4] The Cuts Are Made in a Milling Machine. The Plate Is Stiffened by Two Rails Welded to the Back

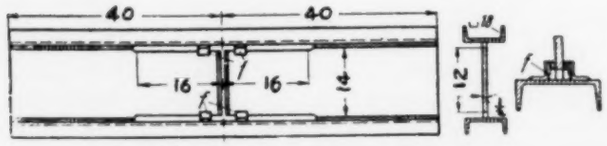


Fig. 14(b)—Type No. 2 Specimen Used by Bierett[4]

The plates are welded to channels, └─┘ 18 = a channel 7 inches wide, 2³/₄ inches leg, 0.32 inch web, 0.43 inch thickness of leg, 40 lb. per yard. f = guide consisting of rollers on each side. Dimensions in inches

The residual stress perpendicular to the weld (measured on a 2-inch gage length spanning the joint) was tension to the extent of 11,000 to 14,500 psi at the ends and 5500 psi in the middle. In the absence of measurements on other gage lengths it is difficult to assess the significance of these results. According to Mies and Bierett, whose results are of an accuracy comparable to Lance Martin's, the edges of restrained, arc-welded plates are much less highly stressed in tension than the middle. Perhaps the reverse run or the directions in which the layers were deposited (no details) may account for Lance Martin's results. The stress parallel to the weld at the section measured (1 inch beyond the center of the weld) was, of course, zero at the edges, but was also zero at the middle, rising to a maximum of 2100 psi in the intervening sections. The residual stress in the side frames perpendicular to the weld varied from 4300 psi tension at the outer side to 38,000 psi compression at the inner side. Apparently the side frames failed by buckling during cooling. Although Lance Martin observed permanent set in the frames after release from restraint, he did not consider the effect of the buckling on the degree of rigidity of restraint.

Using the same types of specimen as Mies[3] and Lance Martin,[18] Bierett[4] also measured residual stresses in restrained three-layer arc welds. The types of specimens are shown in Fig. 14. Four specimens of the first type were made, but only one of the second type, as in Table 11. The welds were 60° V in 0.47-inch mild steel. Type 1 specimens rested on asbestos, but a copper backing-up strip was used. The rate of cooling was not severe. The rails welded to these specimens were for stiffening. The specimens were welded at a Navy Yard or in Railway shops. The first layer of covered electrodes was deposited at 120 amp.; the remaining two at 140 amp. The first layer of the bare electrode was also deposited at 120 amp., but the remaining two at 160 amp. Strain measurements were made with an extensometer of the Siebel and Pfender wedge type (0.79 or 1.18-inch gage length) accurate to ± 1400 psi in 0.79 inch. Measurements were taken after welding, after cutting loose from restraint, and after cutting each gage length from the plate.

The results are shown in Table 11. The stresses given in Table 11 apply to the center line of the weld. As with restrained gas welds, the residual stresses in arc welds determined from the difference in strain readings taken after cutting loose restraint and after cutting out individual strips were the same as those in welds made without restraint. The tensile reaction stress perpendicular to the weld determined from strain readings taken before and after cutting loose the restraint was nearly uniformly distributed over the length of the weld, usually being lower at the start than at the end of the weld. The reaction stress varied from 14,000 to 24,000 psi, and was independent of residual welding stress. Increasing the root spacing from 0.08 to 0.16 inch or using a covered instead of a cored electrode raised the average reaction stress about 2000 psi. However, the welding sequence had a considerable effect on residual stress, as shown in Table 11. In this Table the maximum tensile stresses

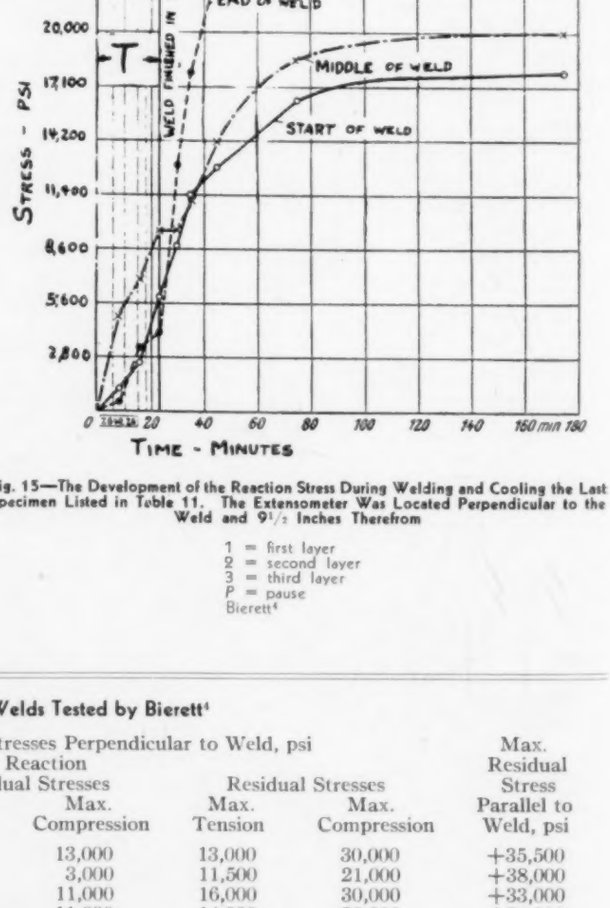


Fig. 15—The Development of the Reaction Stress During Welding and Cooling the Last Specimen Listed in Table 11. The Extensometer Was Located Perpendicular to the Weld and 9½ Inches Therefrom

1 = first layer
2 = second layer
3 = third layer
P = pause
Bierett[4]

Table 11—Restrained Arc Welds Tested by Bierett[4]

Type of Specimen	Root Spacing, Inch	Electrode	Weld Sequence	Stresses Perpendicular to Weld, psi: Sum of Reaction Plus Residual Stresses, Max. Tension	Sum of Reaction Plus Residual Stresses, Max. Compression	Residual Stresses, Max. Tension	Residual Stresses, Max. Compression	Max. Residual Stress Parallel to Weld, psi
A 1	0.08	Covered	→	29,000	13,000	13,000	30,000	+35,500
B 1	0.08	"	←○→	28,500	3,000	11,500	21,000	+38,000
C 1	0.16	"	→	37,000	11,000	16,000	30,000	+33,000
D 1	0.08	Covered 130 amp.	→	33,000	11,000	14,000	30,000	+34,000
E 2	0.12	Bare	→	30,000	15,000	12,000	36,000	+33,000

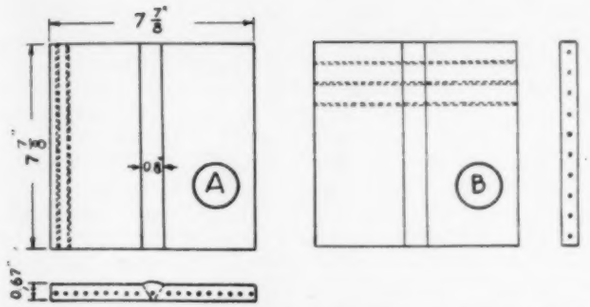


Fig. 16—Specimen *A* for Residual Stress Parallel to Weld. Specimen *B* for Residual Stress Perpendicular to Weld. Dotted Lines Indicate Cuts for Subdivision

Specimen *A*. Holes were 0.39 inch between centers except at the weld. Strips were 0.32 inch wide after cutting.
Specimen *B*. (Same dimensions as *A*.) Holes were 0.78 inch between centers. Strips were 0.71 inch wide. Murata[19]

occur in the vicinity of the middle of the weld; the compressive stresses occur at the ends, the maximum compression usually being at the end last to be welded.

The development of the reaction stress during the welding and cooling of the last specimen in the Table is shown in Fig. 15. The stress was measured on a gage length of 4 inches at a distance of $9^1/_2$ inches from the weld. The time of welding was 23 minutes.

The residual stresses parallel to the weld were near the yield point of base metal, but considerably below the yield point of weld metal (no details). There was practically no difference between these stresses in the free and restrained weld, and the stresses were practically independent of sequence and root spacing. The maximum residual stress parallel to the weld always occurred at the middle of the joint. It should be remembered that all the results on residual stress apply to the weld itself where the maximum stresses are known to occur in butt joints of the type tested by Bierett. Of course, the residual stress parallel to the weld rapidly changes to compression at a few inches beyond the weld, attaining a maximum value of −16,000 psi in the last specimen listed in the Table.

A number of other investigators have measured residual stresses in constrained straight butt joints. Of these, von Roessler[8] and Murata[19] used the subdivision method, and the rest were content to measure values of the "reaction" stress.

The methods and materials used by von Roessler[8] are described in the section on Gas Welds with Restraint. The welds were 60° V and were made in two layers, the first with a 0.12-inch electrode, step-back procedure; the second with 0.16-inch electrodes, continuous welding. The results are shown in Table 12. The stress perpendicular to the weld was believed to increase with increase in heat input to the weld, and, of course, with increase in rigidity of gripping. The residual stress parallel to the weld, however, was decreased as the heated zone available for equalizing the longitudinal shrinkage was increased. Hence, bare electrodes created high residual stress parallel to the weld, but low "reaction" stresses.

Table 12—Residual Stresses in Arc Welds with Restraint. von Roessler[8]

Type of Electrode	Tensile Stress Parallel to Weld, psi	Tensile Stress Perpendicular to Weld, psi: Rigidly Wedged	Tensile Stress Perpendicular to Weld, psi: 0.04-Inch Play at Both Edges
Covered	19,600	30,800	11,200
Bare	25,600	27,900	5,600

Murata[19] also used the subdivision method to determine residual stresses parallel and perpendicular to a 60° V butt weld in mild steel plates containing about 0.25 C. Two welded specimens Fig. 16 were prepared from plates originally 8 x 5 x $^3/_4$ inches, subsequently machined to $7^7/_8$ x $3^7/_8$ x 0.67 inches after annealing $^1/_2$ hour at 840° C. The root spacing was 0.16-inch, shoulder 0.08 inch. Mild steel electrodes (no details) 0.19 inch diameter were deposited in 4 layers after the ends were tacked. The plates were gripped so that they could not bend during welding. Whether end movement was restricted is not stated. The change in gage length was measured before and after welding and after subdivision by means of a Zeiss Optimeter. The residual stress parallel to and in the weld was measured by planing successive 0.04 inch cuts perpendicular to the surface from each side of the 0.78 inch bar comprising weld metal. Changes in gage length were measured after each cut.

Residual stress was computed directly from strain and Young's Modulus, the modulus having been measured on $^1/_2$-inch round tensile specimens machined from welded specimens of the type used for subdivision. The range of tensile properties in specimens cut parallel and perpendicular to the weld is shown in Table 13.

The residual stress parallel and perpendicular to the weld is shown in Fig. 17. The distributions have the usual form but the stresses are remarkably low. The maximum tensile residual stress parallel to the weld is 15,800 psi; the maximum tensile and compressive residual stresses were $^1/_2$ and $^1/_8$ of the elastic limit, respectively. Perhaps the long gage length chosen explains the low results. The distribution of permanent deformation parallel to the weld is shown in Fig. 18. The permanent contraction in the center portions near the weld must have been accompanied by a corresponding increase in thickness or breadth.

The residual stresses in an X weld in rigidly gripped mild steel plates (0.32 x 3.2 x 80 inches) were measured (no details) by Jurczyk[20] by means of a Maihak acoustic extensometer. When the welding was done continuously first on one side then on the other for a length of 40 inches, the maximum residual tensile stress was 17,400 psi. If the welding was interrupted frequently (no details) the maximum residual tensile stress was 6500 psi. The

Table 13—Range of Tensile Properties Parallel and Perpendicular to the Weld. Murata[19]

		Elastic Limit, psi	Tensile Strength, psi	Elongation, % in 4 In.	Young's Modulus, psi
Parallel to Weld	Specimen cut from weld	33,700	51,000	1.7	25,900,000
" " "	Specimens cut from heat-affected zone	30,300 to 39,500	67,000 to 69,000	22.8 to 26.8	30,000,000
" " "	Specimens cut from unaffected base metal in joint	33,700 to 38,500	65,300 to 68,000	26.0 to 31.2	26,500,000 to 33,800,000
Perpendicular to Weld	Specimens cut from ends of welded joint	32,500 to 35,000	38,000 to 53,300	Not det'd	27,100,000 to 29,300,000
" " "	Specimens cut from central portion of welded joint	35,000 to 40,700	50,400 to 55,700	Not det'd	28,500,000 to 32,600,000

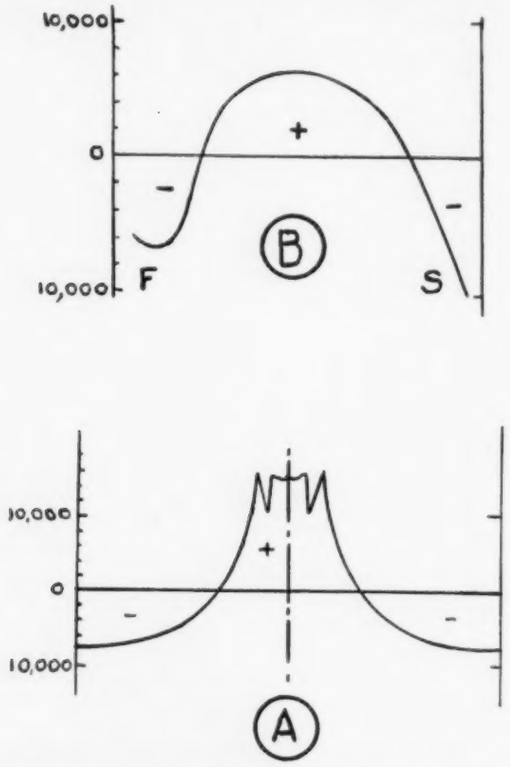


Fig. 17—A-Residual Stress (psi) Parallel to Weld, Specimen *A*
S = start of weld
F = end of weld
B—Residual Stress (psi) perpendicular to weld, Specimen *B*. Murata[19]

amount of metal deposited and the welding conditions were the same in both tests. In the absence of important details these results must be accepted with caution.

As pointed out elsewhere, the permanent distortion in butt welding extends to surprisingly large distances beyond the weld, but the residual stresses outside a zone fairly close to the weld are small and often negligible. If strain measurements perpendicular to the weld are made in the area of gripped plates relatively unaffected by residual stresses, the stresses calculated therefrom with the aid of Young's Modulus are a close approximation to the reaction stress, as Bierett[4] has shown. The results may be nearly as much an indication of the degree of restraint, as of the shrinkage characteristics of the weld.

Determinations of reaction stresses in restrained arc welds by the method outlined above have been made by Gehring,[21] Nikolaev,[15] Orr,[22] Harrelson,[23] Lillicrap,[24] Sarazin[16] and Lottmann.[25]

Extensive tests on reaction stresses were made by Gehring[21] on plates 12 x 6 inches, welded in a 70° V along the 6-inch end, 0.16-inch root spacing. For restraint the other ends of the plates were fillet welded to a rigid foundation. The composition of plates and electrodes is shown in Table 14. Reaction strain was measured by means of Huggenberger tensometers (0.79-inch gage length) placed about $8^1/_2$ inches back of the weld and perpendicular thereto. The temperature rise at this distance from the weld did not exceed 5° C. during or after welding. It should be mentioned that Gehring devised a water-cooled extensometer ($2^3/_8$ inches gage length) for measuring the shrinkage across a butt joint during welding. The results obtained with the instrument are of value only in connection with the particular welding conditions in his tests. No disturbances due to magnetic effects appear to have been observed.

The results for the thin plates are shown in Table 15. The stresses at the edges of the plates were deduced by extrapolation of the measurements made on the three lines in the interior of the plate. The increase of reaction stress with distance from the start of the weld is in agreement with Bierett's results.

Table 15—Reaction Stresses in Arc-Welded Butt Joints 0.24 Inch Thick. Gehring[21]

Electrode	Steel	Reaction Stress, psi At a Distance of ( ) Inches from the Starting End of the Weld 0	$1^1/_4$	3	$4^3/_4$	6
1	Mild	14,200	17,000	20,000	24,000	25,500
2	Low-alloy	10,000	12,000	16,400	17,800	17,800
3	Low-alloy	12,000	13,500	16,400	18,500	18,500
4	Mild	15,700	18,500	22,700	26,500	27,000
5	Mild	16,400	18,500	20,600	26,500	27,000
5	Low-alloy	12,800	15,700	19,200	22,000	23,500

Gehring also found that step-back welding and starting the weld in the middle of its length lowered the re-

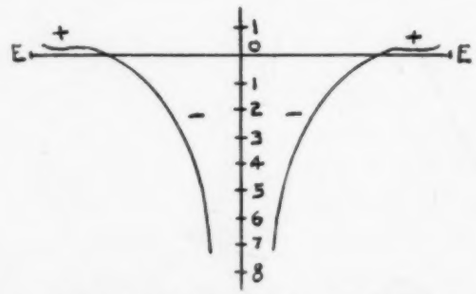


Fig. 18—Permanent Deformation Parallel to the Weld
Specimen A, expressed in thousandths of an inch on a gage length of $7^7/_8$ inches parallel to the weld. The permanent deformation is the difference between the gage length before welding and after subdivision.
E = edge of plate. + = permanent expansion. − = permanent contraction. Murata[19]

Table 14—Plates and Electrodes Used by Gehring[21]

Steel	Thickness Inch	C	Si	Mn	P	S	Cu	Cr	Yield Point, psi	Tensile Strength, psi	Elongation %	Reduction of Area, %
Mild	0.24	0.09	0.01	0.50	0.034	0.026	0.07	0.12	42,500	57,000	28.4	70.5
"	0.39	0.09	0.01	0.44	0.026	0.037	0.16	trace	42,000	54,000	27.5	73.5
Low-alloy	0.24	0.18	0.49	0.93	0.035	0.020	0.51	0.47	60,500	88,500	20.0	58.0
Electrode												
1 bare		0.08	0.01	0.40	0.017	0.025	0.11	..				
2 "		0.13	0.35	0.68	0.017	0.025	0.78	0.38				
3 "		0.30	0.10	2.02	0.019	0.017	0.08	..				
4 coated		0.10	0.01	0.47	0.020	0.033	0.12	..				
5 "		0.09	0.01	0.37	0.015	0.035	0.06	..				

Note: There are slight differences between the analyses given by Gehring and by Schulz and Püngel, who report the same tests. 120–125 amp.: coated electrodes 140 amp.: bare electrodes.

action stress by about 3000 psi. Experiments on the effect of cooling rate with 0.24-inch steel are shown in Table 16. The quenched specimens were immersed in water immediately after welding. The welds in the low-alloy steel were made with electrode 2, those in mild steel with electrode 1. Gehring offers no explanation for the higher reaction stresses found with mild steel compared with low-alloy steel, but concluded that reaction stresses are not related to the time to deposit the weld, or to depth of penetration, and that reaction stresses are decreased by any treatment tending to produce a uniform cooling rate, whether fast or slow, in weld and plate.

Table 16—Effect of Cooling on Reaction Stresses in Butt Welds. Gehring[21]

Kind of Cooling	Reaction Stress, psi: Mild Steel	Reaction Stress, psi: Low-Alloy Steel
Air cooling; steel backing plate	33,500	26,500
Air cooling; asbestos backing plate	....	22,000
Air cooling; copper backing plate	....	28,000
Water quench; copper backing plate	28,300 (*B*)	19,000 (*A*)

A = time of welding was 77.5 seconds
B = time of welding was 122.5 seconds

Nikolaev[15] measured residual stresses in arc-welded joints under two different types of restraint, using the method and materials described in the section on Arc Welds Without Restraint. The first type of restraint consisted of three equally spaced $1^3/_4$-inch fillet welds on both sides of each plate, which secured the plates to a rigid foundation (no details). The second type of restraint consisted of the same side-fillet welds as in the first type, but in addition three similar fillet welds along the end of each plate. Plates tacked together at the ends over a length of $^3/_4$ to 1 inch were also tested.

The results are summarized in Table 17 and apply to d.c. welding of 0.43-inch plate. The residual stresses plotted by Nikolaev show erratic tendencies, but point to the undesirability of any form of restraint from the standpoint of residual stresses. Since Nikolaev did not use the subdivision or other methods for determining residual stresses his values for the restrained specimens consist partly of reaction stresses and partly of residual stresses. The two effects cannot be separated in the tabulated results.

Orr[22] endeavored to provide effective restraint for his arc welds in mild steel $^1/_2$ inch thick by welding them to a steel billet 3 inches thick by means of heavy fillets (no details). The welds were made with 3 runs of 10-gage electrodes followed by 2 runs of 6-gage, cooled between runs. The length between restraining fillets was either 9 or 58 inches. Reaction stress was computed from strain gage readings after welding, and after cutting free from restraint.

The results are shown in Table 18.

Table 18—Reaction Stresses in Arc Welds. Orr[22]

Free Length, Inches	Type of Welding	Tensile Reaction Stress, psi
9	Slow	29,000
9	Fast	27,000
58	Slow	16,000
58	Fast	9,500
9	Peened cold	10,000
9	Peened hot	11,000

As the free length is increased the reaction stresses decrease. It was suggested that bending might have affected Orr's results, but Orr observed no lateral movement or bending in the finished welds. Orr proposed the

$$\text{formula } S = \frac{540{,}000}{L}.$$

where S = welding stress, psi and L = free length of plate in inches, for values of L over 20 inches, for butt welds in mild steel. The decrease in reaction stress by peening is noteworthy, but one hot-peened specimen cracked.

The effect of peening on shrinkage strains and stresses in 90° X arc welds (specimen with integral restraining side frames) in hot-rolled low-carbon steel plate $1^1/_4$ inches thick was studied by Harrelson.[23] The welds were only $1^1/_8$ to 2 inches long, and the mid-points of the gage lengths for the 2-inch Berry gage were only $3^1/_2$ inches from the center of the weld. The locked-up stresses varied from 29,000 to 66,000 psi in peened specimens and from 37,000 to 99,000 psi in unpeened. (Note: it is not clear whether Harrelson's stresses were tension or compression. He implies they were compression, but it seems more reasonable to suppose that they were tension.)

Lillicrap[24] used exactly the same arrangement (58-inch free length) as Orr for determining reaction stresses except that the rigid foundation was a wrought-iron plate 9 inches thick. Tests were made on $^1/_4$-inch and $^1/_2$-inch ship steel with heavy coated electrodes (no details), the joint being tacked at both ends. Strain was measured perpendicular to the weld on a gage length of 20 inches, starting about 6 inches, back of the weld. The strain in the $^1/_2$-inch plate was measured after each of six runs with intermediate cooling. The strain increased during each run and cooling, and attained a final value equivalent to a tensile stress of 18,000 to 20,000 psi.

Table 17—Residual Stresses Perpendicular to Weld in Constrained V Butt Joints. Nikolaev[15]

Type of Restraint	Diameter of Electrode, Inch	Current Amp.	Weld Sequence	Type of Electrode	Max. Tensile Stress, psi	Max. Compressive Stress, psi
6 side fillets	0.20	190–200	→ ←	Chalk covered	Above yield point	Above yield point
" " "	0.16	150–160	8 7 6 5 ⇄ ⇄ ⇄ ⇄ 1 2 3 4	Heavy covered	28,200	10,000
" " " and 3 end fillets	0.16	160–180	→ ←	" "	Above yield point	500
" " " " " " "	0.16	150–160	8 7 6 5 ⇄ ⇄ ⇄ ⇄ 1 2 3 4	" "	" " "	13,000
Tacked	0.16	150–160	Ditto	Chalk covered	" " "	Above yield point

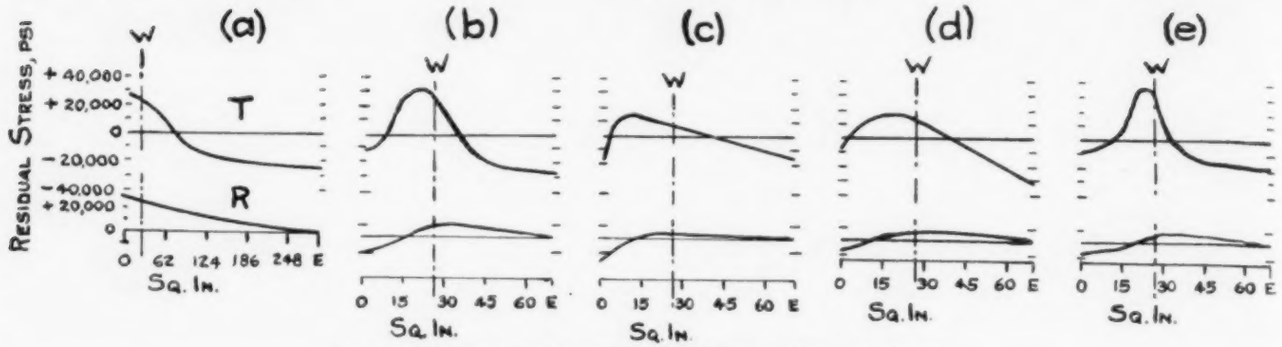


Fig. 19—Residual Stresses in Disks with Arc-Welded Patches. Bühler and Lohmann[7]

Upper diagrams — tangential residual stress
Lower diagrams — radial residual stress
W = location of weld
Abscissas are cross-sectional area of disk within section under consideration.

E = edge of plate = $\frac{\pi}{4}(9.6)^2$ = 75 sq. in. for disk 9.6 inches diameter.

(a) Patch 5.9 inches diameter welded in mild steel disk (St 34) 19.6 inches diameter 0.16-inch covered electrode; continuous weld; tacked.
(b) Patch 5.9 inches diameter welded in mild steel disk (St 34) 9.6 inches diameter; 0.16-inch covered electrode continuous weld; untacked.
(c) Patch 5.9 inches diameter welded in mild steel disk (St 34) 9.6 inches diameter; 0.16-inch covered electrode; continuous weld; untacked; each layer peened.
(d) Patch 5.9 inches diameter welded in mild steel disk (St 34) 9.6 inches diameter; 0.16-inch covered electrode; back-step welding; untacked.
(e) Patch 5.9 inches diameter welded in mild steel disk 9.6 inches diameter; 0.16-inch covered electrode; back-step weld; untacked; plate cooled during welding.
In all cases the electrode contained 0.25 C, 0.33 Si, 0.70 Mn, 0.026 P, 0.024 S.

Sarazin[16] also measured residual stresses in butt-welded plate fillet welded to a rigid foundation (no details). A dial gage extensometer accurate to 1400 psi in 1-inch gage length was used. In a 60° V weld in mild steel plates 0.32 inch thick and 10 inches square tensile stresses of 43,000 to 50,000 psi (above the yield point) were observed 1 inch back of the weld. Permanent distortions as large as +0.05 inch per inch were observed 8 inches from the weld.

Like Orr, Lottmann[25] found that an increase in length of plates between rigid grips decreased the reaction stress, whereas thickness of plate had little effect. In welding two plates 0.20 inch thick along the 20-inch edge with bare electrodes, and 0.20-inch root spacing, the reaction stress decreased with increase of length between the gripped ends as shown in Table 19. The restraint was created by fillet welding to a rigid foundation. Lottmann and Orr are in agreement that the reaction stress with rigid gripping is inversely proportional to the free length, provided the weld occupies an insignificant part of the length. The proportionality constant is 540,000 in Orr's experiments, but 2,000,000 in Lottmann's. Neither investigator studied the effects of varying rigidity or of slight buckles or slack in the plates.

Table 19—Effect of Free Length on Reaction Stresses. Lottmann[25]

Length Between Gripped Ends, Ends, Inch	Tensile Reaction Stress, psi
20	103,000
40	51,500
80	25,500
160	12,800
390	5,100

Arc Welds with Restraint (Patches)

The residual stresses in disks with arc-welded patches have been measured by Bühler and Lohmann,[7] who used the Sachs Method and specimens described in the section on Oxyacetylene Welds with Restraint. The steels used are listed in Table 20. Four covered electrodes were used containing 0.22 to 0.25 C, 0.25 Si, 0.8 Mn. Two bare electrodes were used; one contained 0.10 C, 0.12 Si, 0.68 Mn; the other 0.30 C, 0.10 Si, 2.02 Mn; both contained 0.08 Cu. There was little difference among the electrodes, which were 0.16 or 0.24 inch diameter. Sometimes one electrode caused high residual stresses, sometimes low. Therefore, the results given in this summary are average although the residual stresses created by different electrodes in otherwise identical specimens varied up to 400% (18,500 psi). The welds were 70° X, three layers on each side.

Table 21 summarizes the results. As with the oxyacetylene welded patches there was a great difference between the types of stress distributions obtained with the large and small disks, Fig. 19.

Conclusions for 9.6-Inch Disks.—In mild steel (St. 34) tacking was of slight benefit, but back-step welding (tacked or not) was of no advantage. Peening (no details) decreased the residual stresses by 40 to 50% and had greater effect with covered than with bare electrodes. Peening the hot weld relieves the elastic stresses by causing plastic strain; only the first layers should be peened. Peening did not appear to change the type of residual stress distribution. Water cooling the plates during welding increases residual stresses by increasing the temperature gradient. That is to say, all the welding heat is restricted to a zone adjacent to the weld so that shrinkage during cooling exerts its principal effect on the weak metal in the immediate vicinity of the weld.

Residual stresses in the *stronger mild steel (St 37)* were slightly higher than in the weaker mild steel (St 34).

With *low-alloy steel* the larger electrodes usually created lower residual stresses than the smaller. Back-step welding was of some advantage, but there was no dif-

Table 20—Steels Used by Buhler and Lohmann[7]

Steel	Chemical Composition, % C	Si	Mn	P	S	Cr	Cu	Upper Yield Point, psi	Lower Yield Point, psi	Tensile Strength, psi	Elongation % in 10 Diam.	Reduction of Area, %
Mild (St 34)	0.08	0.01	0.41	0.017	0.014	0.03	0.10	32,000 to 33,000	29,800 to 31,800	52,000	28.7 to 30.1	67 to 72
Mild (St 37)	0.11	0.01	0.47	0.029	0.023	0.01	0.04	38,400	37,400	61,000	27.2	66
Low-Alloy	0.15	0.50	1.05	...	...	0.34	0.60	54,000	53,000	81,000	26.4	65

Table 21—Residual Stress in Arc-Welded "Patches." Bühler and Lohmann[7]

Outside Diameter of Disk, Inches	Steel	Electrode Diameter, Inch	Welding Details	Max. Tensile Stress in Tangential Direction, psi	Tangential Compression at Outer Edge, psi	Tensile Stress in Weld, psi	
						Tangential	Radial
9.6	Mild (St 34)	0.16 inch	Continuous weld, untacked	21,400	28,500	20,000	5,700
9.6	Mild (St 34)	0.16 inch	Step-back, untacked	22,800	24,200	12,800	7,100
9.6	Mild (St 34)	0.16 inch	Continuous, tacked	18,500	35,700	14,200	7,100
9.6	Mild (St 34)	0.16 inch	Step-back, tacked	24,200	30,000	20,000	7,100
9.6	Mild (St 34)	0.16 inch	Continuous, untacked, each layer peened	11,400	17,100	4,300	2,800
9.6	Mild (St 34)	0.16 inch	Step-back, untacked, water cooled	38,500	28,500	35,700	7,100
9.6	Mild (St 37)	0.16 inch	Continuous, untacked	25,700	22,800	21,300	8,600
9.6	Low-Alloy	0.16 inch	Continuous, tacked	30,000	41,300	27,000	10,000
9.6	Low-Alloy	0.16 inch	Step-back, tacked	30,000	38,500	27,000	10,000
9.6	Low-Alloy	0.16 inch	Continuous, tacked, water cooled	57,000	41,300	56,000	12,800
9.6	Low-Alloy	0.16 inch at root; others 0.24 inch	Continuous, tacked	34,200	37,100	24,200	10,000
9.6	Low-Alloy	0.16 inch at root; others 0.24 inch	Continuous, tacked, each layer peened	10,000	24,200	8,600	4,300
9.6	Low-Alloy	0.16 inch at root; others 0.24 inch	Step-back, tacked	27,100	37,100	18,500	10,000
9.6	Low-Alloy	0.16 inch at root; others 0.24 inch	Continuous, tacked, water cooled	47,000	38,500	35,700	12,800
19.6	Mild (St 34)	0.16 inch	Continuous, tacked	27,100	24,200	21,300	21,400
19.6	Mild (St 34)	0.16 inch	Step-back, tacked	35,700	24,200	17,100	24,200
19.6	Mild (St 34)	0.16 inch	Continuous, tacked, water cooled	31,400	27,000	20,000	22,800
19.6	Mild (St 34)	0.16 inch at root; others 0.24 inch	Continuous, tacked	32,800	27,000	20,000	22,800
19.6	Mild (St 34)	0.16 inch at root; others 0.24 inch	Step-back, tacked	27,100	24,200	15,700	18,500
19.6	Mild (St 34)	0.16 inch at root; others 0.24 inch	Step-back, tacked, water cooled	25,700	24,200	17,100	20,000
19.6	Low-Alloy	0.16 inch	Step-back, tacked	58,500	32,800	42,800	38,500
19.6	Low-Alloy	0.16 inch	Single layer weave	44,200	35,700	27,000	37,100
19.6	Low-Alloy	0.16 inch	Weave; plate curved 0.20 inch before welding	37,100	28,500	22,800	32,800
19.6	Low-Alloy	0.16 inch at root; others 0.24 inch	Continuous, tacked	62,500	42,800	27,000	50,000
19.6	Low-Alloy	0.16 inch at root; others 0.24 inch	Step-back, tacked	64,000	41,400	22,800	50,000
19.6	Low-Alloy	0.16 inch at root; others 0.24 inch	Continuous, tacked, each layer peened	48,500	41,400	25,700	32,800

NOTE: The method of water cooling the plates during welding is not described. The single-layer weave means welding back and forth in short sections to complete one side of the joint in a single pass.

ference between covered and bare electrodes. Peening decreases residual stresses; water cooling increases them. The low-alloy steel generally had up to 50% higher residual stress than the mild steel with equivalent treatment.

Conclusions for 19.6-Inch Disks.—In mild steel (St 34) back-step welding was advantageous with 0.24-inch electrodes but was disadvantageous with 0.16-inch electrodes. Water cooling during welding was not nearly so damaging as with 9.6-inch disks. This is considered another illustration of the close relation which exists among cooling velocity, size of specimen, and residual stresses. Pre-bending the disk before welding decreased residual stresses by 16%. In the low-alloy steel there was an especially large increase in residual stress as the outside diameter of the disk was increased.

Siebel and Pfender[5] welded a patch $2^3/_4$ x $3^1/_8$ inches with double-beveled edges into the center of a mild steel plate 16 x 18 x 1.18 inches using 0.16-inch bare electrodes, 230 amp. The distribution of residual stresses, measured by subdivision method described in the section on Arc Welds Without Restraint, was the same as in a plate heated in the center to 400° C. (See section on Local Heating by Torch.) A maximum tensile stress of 23,000 psi was found near the patch; the edge of the plate was under a residual compressive stress of 23,000 psi. Tensile stresses were lower on the face of the plate that was welded first.

The Mathar Method, according to Bollenrath,[2] revealed residual tensile stresses up to 100,000 psi in the weld securing a patch 6 inches square in the center of an annealed mild steel plate 24 inches square. The weld was a tacked V made with covered electrodes, 2 layers 0.16 inch diameter, 2 layers 0.20 inch diameter (no details). Bollenrath believed that residual stresses increase with rigidity of restraint.

The effect of preheating on residual stresses in restrained arc welds is illustrated by Sonderegger's[26] experiments. Butt welds (V) in mild steel plates 8 inches square, 2 inches thick, fillet welded to a rigid foundation broke loose before welding was completed if not preheated. If preheated to 300° C., the joint did not break loose from the support, and showed no distortion after it was chipped from the support (no details).

Tensile, bend and notch impact specimens cut from oxyacetylene or arc-welded butt joints with restraining "side frames" had the same properties as specimens cut from oxyacetylene and arc welds made without restraint, according to Hochheim[27] and Buchholz.[28] Since the physical test specimens probably had only a small amount of residual stress and no reaction stresses, the

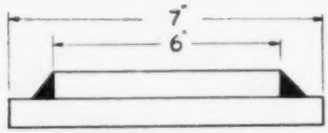


Fig. 20—Specimen Used by Cantrell.[34] Plates 1/2 Inch Thick, 30 Inches Long. The Ends of the Plates Were Fixed

results can be interpreted as showing simply that reaction stresses, which may have been above the yield point, had no harmful effect on the structure of mild steel, or low-alloy structural steel. Similar investigations on the effect of residual stresses on the notch-impact value of specimens cut from welds in free and restrained plates are reported by Schuster[29] (Dec. 1936) as being under way.

Carbon Arc Process

The following investigations of residual stresses in steel that has been carbon-arc welded have been reported.

1. Lawson[30] (see section on Fillet Welds), fillet-welded rail joints.
2. Sandelowsky[31] (see section on Welded Boilers), carbon-arc welded drum.
3. Bollenrath[2] (see section on Beads Deposited on Surface), bead of bare mild steel wire deposited on mild steel plate.

The Effect of Thickness

Several investigators mentioned in preceding sections observed that the residual stresses in the plane of butt-welded plates were different on the root side from the face side. For example, Mailänder,[32] applying the Stäblein method of milling off successive surface layers to V welds (no details) in mild steel 1.6 inches thick, found that the root was in tension, the face in compression and that in the intermediate region the residual stresses underwent three reversals of sign. A. M. Roberts[33] states that, as a general rule, the surface layers at opposite faces of a butt weld (no details) are in opposite states of stress. He believes that between these opposite surface stresses there are two reversals of stress and a region at or near the middle which carries no residual stress. On this belief he bases his rule that internal weld defects are less harmful than surface defects.

Using his modified Mathar Method, Bollenrath[2] observed the differences shown in Table 22 between the root and face side of V weld 25 inches long made with bare or covered electrodes in mild steel 0.59 inch thick, 12 inches wide. Young's Modulus was 30,000,000 psi. The low values observed after the second cut are explained partly by inelastic deformation caused by the first cut. Presumably the results of the third cut are regarded as most accurate.

Apart from the effect of thickness in altering the distribution of residual stresses in the plane of the welded plates, the residual stress perpendicular to the plane of the plate becomes appreciable as plate thickness increases. The only information on the effect appears to be given by Mesmer.[10] He showed that flow figures appeared on Fry etching the cross section of a weld (no details) in mild steel 0.59 inch in which a Mathar hole 0.47 inch diameter had been drilled. The flow figures were caused by the great difference between the small principal stress in the plane perpendicular to the welded plates and the principal stresses in the plane of the plate. Similar flow figures, sometimes twice as long as the diameter of the hole, were also observed on the surface of butt-welded plates. They were caused by stress concentration around the hole.

Table 22—Residual Tensile Stresses Parallel to Weld on Root and Face Sides. Bollenrath[2]

Length of Cuts, Inches Modified Mathar	Residual Stresses, psi, Bare Electrodes		Residual Stresses, psi, Covered Electrodes	
	Face	Root	Face	Root
0.8	81,000	60,000	71,000	48,500
1.6	50,000	51,000	10,000	11,400
3.6	56,000	46,000	57,000	50,000

Fillet-Welded Joints

There is little information on the distribution and magnitude of residual stresses in fillet-welded joints (see section on Residual Stresses in Welded Beams). Sarazin[16] found that the residual stress in the plates of a double end fillet-welded lap joint in mild steel 0.39 inch thick welded in three layers with 0.16-inch electrodes, 140 amp. (no details) was tensile in a direction perpendicular to the weld, but compression parallel to the weld.

Cantrell[34] measured the residual stress created by fillet welding two rigidly gripped mild steel bars 30 inches long, Fig. 20. Extensometer measurements made before and after welding and cooling indicated a tensile stress parallel to the welds of 14,400 psi in the 6-inch plate and 10,100 psi in the 7-inch plate. The difference was attributed to bending. Somewhat similar measurements were made by Griffin, but the results are not at all conclusive. Orr stated that fillet joints welded under restraint create only 50% of the reaction stress of equivalent butt welds (no details).

The residual stress around a fillet-welded reinforcing strap of Mefi type over a butt weld in mild steel plate 0.59 inch thick was measured by Möller and Barbers,[35] using the X-ray method. At each of 7 points the parameter was measured before and after drilling. The drilling was done with a hollow drill producing a core 0.16 inch diameter from which the residual stresses had been removed. Residual stresses thus determined varied from 64,000 psi compression in the butt weld to 137,000 psi tension in the plate just outside the end fillet weld of the strap. The latter value was far above the yield point (no details), but it was thought that a multi-axial stress condition might partly account for the result.

T Joints

A useful indication of the residual stresses parallel to the weld in double-T joints, Fig. 21, is provided by

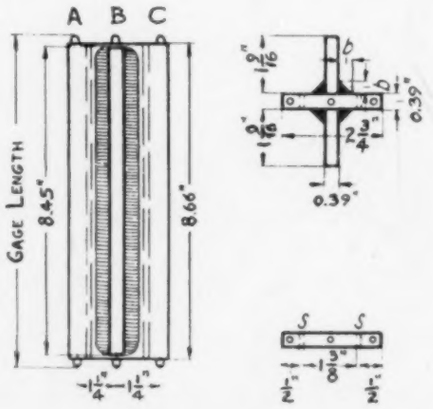


Fig. 21—Double-T Joint Used by Graf[26]

S are saw cuts
b = leg of fillets
A, B and C are gage lengths

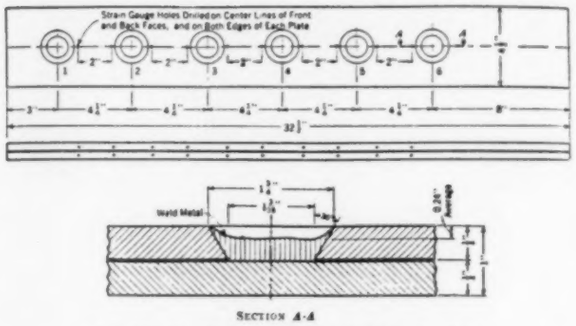


Fig. 22—Plug-Welded Specimen Used by Roark[37]

Graf's[35] measurements. The specimens were welded with bare electrodes, 170 amp. d.c., 17 volts. The legs of the fillets were either 0.20 inch (single layer) or 0.39 inch (three layers). Measurements of the three gage lengths *A*, *B* and *C*, were made before and after welding, after cutting off the welds and the two shorter strips of the T, as well as after making saw cuts 0.51 inch from each edge of the remaining bar.

The stresses calculated from Young's Modulus and the change in gage length as a result of welding and complete cutting are shown in Table 23. The tabulated stresses are, as Graf points out, only the average parallel to the weld over the entire length of the specimen. It seems not unreasonable to consider the results as indicating that the welds were themselves in tension before their removal.

Table 23—Residual Stresses in T Joints. Graf[36]

Type of Welding	Fillet Size, Inch	No. of Layers	Residual Stress, psi (+ = tension) (− = compression) *A*	*B*	*C*
Arc	0.20	1	−13,000	+15,600	− 9,100
"	0.39	3	−11,700	+ 9,100	−14,300
Oxyacetylene	0.43	1	− 5,500	+ 2,800	− 4,300
"	0.67	2	− 4,800	+ 4,300	− 5,200

The residual stresses in arc-welded single-T joints in steel having a tensile strength of 71,000 psi were measured by Sarazin,[16] using the direct method without subdivision. The welds tend to close the angle between the plates, the outer surface of the flange of the T being in tension, the inner surface being in compression (43,000 to 50,000 psi). Although Sarazin was able to obtain readings as close as $^1/_4$ inch from the weld, the stresses he determined are in all probability fictitious, because the strains were probably partly inelastic.

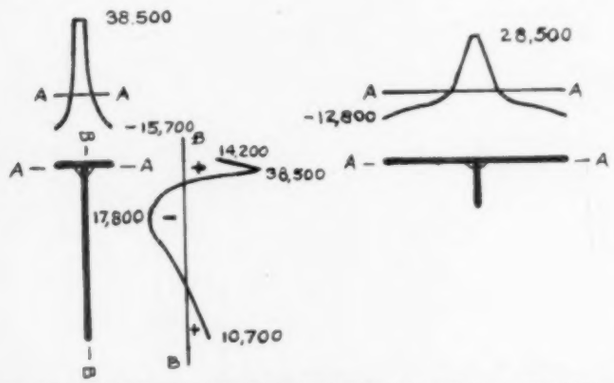


Fig. 23—Residual Stress (psi) Parallel to Fillet Welds Joining Flange to Web of Arc-Welded T beams

Left: flange: 4 x 0.59 inch; web: 12 x 0.39 inch; throat of weld: 0.16 inch
Right: flange: 13 x 0.51 inch; web: 3 x 0.28 inch; throat of weld: 0.30 inch
Bierett and Gruning[40]

Plug Welds

The residual stresses in plug (or button) welded plates Fig. 22 have been measured by Roark.[37] Hot-rolled bars containing 0.10 to 0.20 C, 0.30 to 0.60 Mn (A. S. T. M. Specification A-107-30) were used, and were annealed 1 hr. at 1100° F. (590° C.) after cutting and drilling. Strain gage holes were then drilled. Eight specimens *A* to *H* were welded in the following sequence: holes 1 and 5 were welded and allowed to cool for 30 minutes; then holes 3 and 6 were welded and allowed to cool for 30 minutes, finally holes 2 and 4 were welded. Two specimens, *E* and *F*, were not allowed to cool between alternate plugs. Two other specimens, *G* and *H*, were annealed at 1100° F. (590° C.) for one hour. Shielded-arc electrodes (Fleetweld) $^5/_{16}$ inch diameter, 300 amp., were used. The holes were welded to a depth of $^3/_8$ inch. The physical properties of the duplicate specimens are shown in Table 24 *A*. Strain gage readings before and after welding and after subdivision were reduced to average stress by means of Young's Modulus.

The calculated residual stresses are shown in Table 24 *B*. Although the results are irregular even for specimens *A* and *C* which were presumably identical, welding with intermediate cooling gives lower residual stresses than without. The stress in the intervals between welds was compressive in the back plate, tensile in the front plate. The well defined break in the stress-strain curve for the welded specimens at their low proportional limit was believed to indicate that a considerable volume of metal reached its elastic limit at the same time, and, therefore, must have been under an approximately uniform state of residual stress.

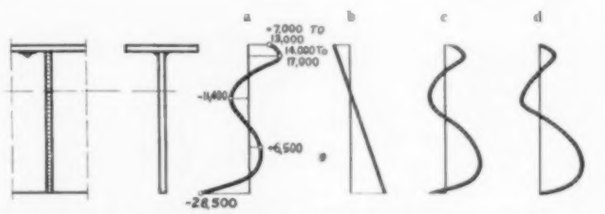


Fig. 24—Residual Stress (psi) Parallel to Fillet Welds Joining Flange to Web of Arc-Welded T Beams at a Butt Weld in the Web

Flange = 6 x 0.59 inch. Web = 11 x 0.39 inch. Throat of fillet = 0.16 inch
(*a*) Residual stress in the butt weld
(*b*) Bending stress
(*c*) Combined residual welding stress due to external load of 20,000 psi in extreme fiber.
(*d*) Combined residual welding stress plus bending stress due to external load of 28,500 psi in extreme fiber. Bierett[38]

Strain gage readings on 0.39 inch mild steel plates before and after making a plug weld in the center 0.87 inch diameter are reported by Sarazin.[16] At a distance from the weld the base metal had elongated which suggests that radial compressive residual stress was present. The high strains at the center were doubtless made up of elastic and inelastic parts.

Welded Beams

The determination of residual stresses in several types of welded mild steel T beams has been investigated by Bierett,[38,39,40] who does not describe his methods of strain measurement and welding in detail. The distribution of residual stress in two welded T beams 5 feet long is shown in Fig. 23. The flange was welded to the web by means of a single layer of 0.16-inch covered or cored electrodes. Residual stresses decrease as the flange width is increased. The corresponding distribution for a T beam with butt-welded web, double V, is shown in

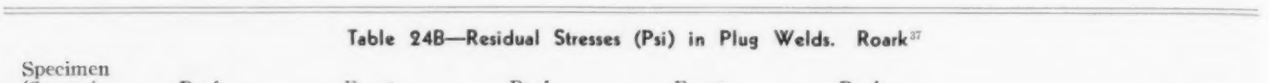
Table 24A—Physical Properties of Plug-Welded Specimens. Roark[37]

Specimen Tested For In Tension	Specimen Tested For Residual Stress	Young's Modulus, psi Welded	Young's Modulus, psi S. T.	Proportional Limit, psi Welded	Proportional Limit, psi S. T.	Yield Point, psi Welded	Yield Point, psi S. T.	Stress at Which Flaking of White Wash Was Observed, psi
B	*A*	29,000,000	29,000,000	13,250	29,600	32,450	33,750	32,200
D	*C*	29,250,000	28,300,000	14,250	29,600	33,000	33,750	36,700
F	*E*	27,000,000	29,300,000	13,250	27,000	33,700	34,200	36,400
G	*A* and *C*	.....	.....	27,250	....	30,000		....
H	*A* and *C*	.....	.....	30,000	....	32,500		....

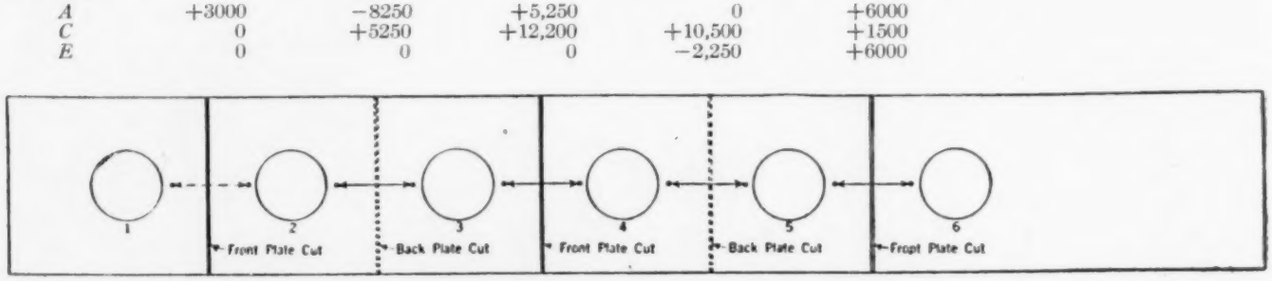
S. T. = standard tensile specimen cut from base metal.

Table 24B—Residual Stresses (Psi) in Plug Welds. Roark[37]

Specimen (Stress in Upper Edge)	Back Plate	Front Plate	Back Plate	Front Plate	Back Plate
A	−2,250	+2,250	0	−750	−6,750
C	−1,500	+12,000	−4,500	+8250	0
E	+10,500	−7,750	−10,500	−1500	+22,500



(Stress in Face)					
A	+3000	−8250	+5,250	0	+6000
C	0	+5250	+12,200	+10,500	+1500
E	0	0	0	−2,250	+6000



(Stress in Lower Edge)					
A	−3,000	+8,250	−750	0	−750
C	−1,500	+3,000	+3,750	0	−9,750
E	0	+8,250	−4,500	0	+2,250

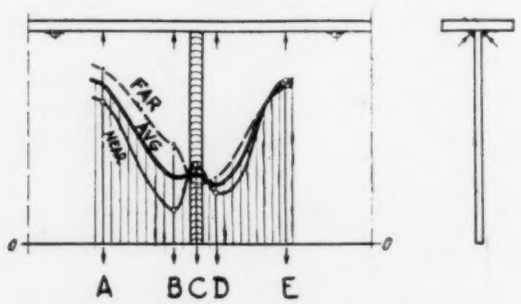


Fig. 25—Residual Tensile Stresses in and Parallel to Fillet Welds (0.16-Inch Throat Joining Flange to Web in the Vicinity of a Butt Joint in the Web). Bierett[28]

Web = 0.39 x 11.2 inches
Flange = 0.59 x 4 inches
Residual Stress (psi) at:

	A	*B*	*C*	*D*	*E*
Near weld	28,900	7,000	16,400	10,600	33,400
Far weld	35,500	20,000	11,600	13,400	31,600
Average	32,200	13,500	14,000	12,000	32,500

Fig. 24. The butt weld induces high compressive stress in the tension area of the web. Fig. 25 shows the decrease in residual tensile stress in the fillet weld joining flange to web in the vicinity of the butt. The residual stress distribution is not improved to any extent by depositing beads on the surface of the tension area of the web, Fig. 26.

Gerold and Müller-Stock[41] measured the longitudinal residual stresses (usually near the mid-section) in welded I beams 55 inches long made up of two flange plates and a web plate of different dimensions with stiffeners 8 inches from each end. Low-alloy steel electrodes 0.20 inch diameter or copper-coated low-alloy steel oxyacetylene welding rods were used. The subdivision method and Siebel and Pfender's wedge extensometer were used, measurements being made at 12 points. Gerold and Müller-Stock showed that a disadvantage of the method is that, if the stresses are not uniformly distributed over the thickness of web and flanges, internal stresses remain in the cut section.

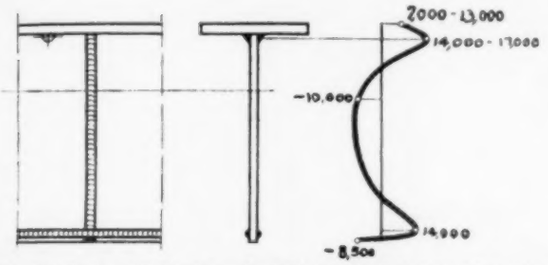


Fig. 26—Distribution of Residual Stress Parallel to Longitudinal Axis of Butt and Fillet Weld T Beam with Reinforcing Beads Near Tension Fiber. Distribution Applies to Butt Weld

Fillet welds = 0.16 inch throat. Web = 0.39 x 11.2 inches. Flange = 0.59 x 6 inch Beam was statically loaded to 25,700 psi (tension fiber) and unloaded before residual stresses were determined. Bierett[38]

To determine whether residual stress varied along the length of a beam, a mild steel beam 12 inches deep, 10 inches wide, nose profile flanges, continuous arc welds was cut through at six sections along the length. The residual stresses were practically the same at all sections. Measurements were also made at 30 points along the center line of the flanges of two beams. The residual stress varied from +10,000 psi to −10,000 psi.

The effect of non-uniform residual stress distribution across the thickness of the flange, Fig. 27, was determined by splitting in half the 0.59 inch thick nose profile flange of a gas-welded low-alloy structural steel beam 18 inches deep, 10 inches wide, continuous fillets. The residual stress was changed by the addition of a constant amount of compression (no details) throughout the thickness of the flange but the type of stress distribution was unaltered.

The comparison of flat with nose profile flange plates was made on low-alloy structural steel beams 12 inches deep, 10 inches wide, using step-back arc welding. The outer surfaces of the nose profile flanges were in compression, maximum (14,000 to 28,500 psi) at the edges and minimum (even slight tension) at the center line of the flange. The un-ribbed flange was also in compression at the edges (28,500 psi) but there was high-tensile stress (18,500 psi) along the center line. The depth of the beam ($5^1/_2$ to 16 inches) had no effect. The central sections of the web were also in compression (up to 36,000 psi, average 7000 to 14,000 psi), but the welds and their vicinity were in tension (43,000 psi maximum for both types of flanges, but the average was about 35,000 psi for the plain, and 15,000 for the nose profile). In the nose profile flange the tension in the contracting weld area is absorbed by the ribs so that the outer surface of the flange remains in compression. In all cases the low-alloy structural steel (74,000 psi tensile strength) contained higher residual welding stresses than the mild steel (53,000 psi tensile strength).

The webs of oxyacetylene welded beams with continuous fillets and nose profile flanges, were in tension throughout (28,000 to 35,000 psi with mild steel, 43,000 to 50,000 with low-alloy structural steel). With plain flanges in both steels, oxyacetylene welding produced tension in the weld, but compression in the middle of the web. Depending on the depth of the beam, arc or gas processes gave the higher residual stresses. Shallow oxyacetylene welded beams had relatively low residual stress.

By bending the flange plate of an arc-welded beam to an initial concavity there was a 10% reduction in residual stress. There was no difference between continuous and step-back welding.

The effect of a stiffener in an arc-welded mild steel beam with nose profile flanges was to increase tensile stress in the center line of the flange to 50,000 psi above the stiffener. On the flange being split in half, this stress fell to 36,000 psi, indicating non-uniform stress distribution through the thickness of the flange. In oxyacetylene welded mild and low-alloy steel beams, 12 inches deep, 10 inches wide the stiffener created tensile stress in the inner side of the nose profile flange. At a distance from the stiffener the inner side was in compression.

Residual stress measurements on rolled I beams and a rolled H column convinced Gerold and Müller-Stock that there is no basic difference between the residual stresses in rolled and welded beams. Dörnen[42] was led to the same conclusion. He used the subdivision method to study longitudinal residual stress in the flanges of continuous welded mild steel plate girders 13 feet long. The web was 0.39 inch thick, 31 inches deep. The width of the flange was 8 inches. The throat of the fillet weld was either 0.20 or 0.35 inch. The former was made in one pass with 0.16-inch dipped mild steel electrodes. The latter was made in two passes, the first layer with 0.16-inch electrodes, the second with 0.20-inch electrodes. Welding was done in a horizontal position simultaneously by two welders on opposite sides of the web. One welder was 8 inches ahead of the other. The first layer was lightly peened with a compressed air hammer. The second layer was begun only after the first had cooled to room temperature.

Dörnen's average results are given in Table 25. Residual stresses are increased by thick beads and thin flanges. Tensile residual stress occurred in the vicinity of the fillet welds. An as-rolled beam removed from service was found to contain higher internal stresses than the welded beams.

According to Bühler,[189] the residual stresses in a continuous fillet-welded plate girder composed of a web

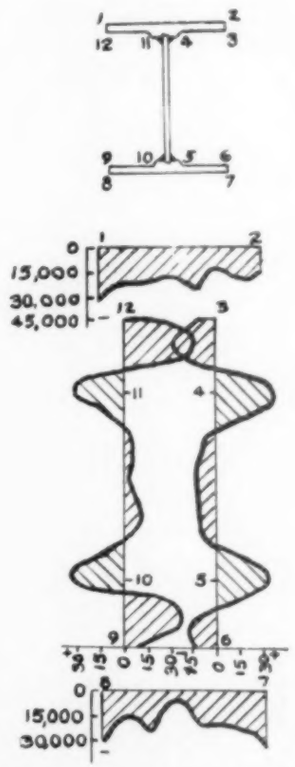


Fig. 27—Residual Stresses (psi) Parallel to the Length in the Middle of an Arc-Welded Beam of Low-Alloy Structural Steel (Nose-Profile Flanges) with Continuous Fillet Welds Joining Web to Flanges. The Positions of the Twelve Extensometers Are Indicated by Numerals

The beam is 12 inches high, 10 inches wide, 55 inches long. Gerold and Müller-Stock[41]

Table 25—Longitudinal Residual Stresses in the Web of Arc Welded Plate Girders. Dörnen[42]

Thickness of Flange, Inch	Throat Thickness, Inch	Residual Compressive Stress, psi
0.39	0.20	7300
0.39	0.35	9800
1.57	0.20	570
1.57	0.35	2000

plate 55 inches high, 0.47 inch thick and two flange plates 14 inches wide, 1.18 inches thick are: first flange welded: 11,300 psi maximum compression in direction of axis of girder on outside surface at center line of web; second flange welded: 24,500 psi maximum compression in direction of axis. The web itself had residual stress of 7700 psi tension near the first welds and 5200 psi compression near the second welds. The residual stresses induced in a rolled I beam by a reinforcing plate 10 inches wide, 0.79 inch thick continuous filled welded to one flange were 2300 psi compression in the welded flange and 1300 psi tension in the unwelded flange. (Details of these tests were not available to the reviewers.)

Süs[43] plotted time during welding *vs.* the dimensional changes indicated by a 40-inch extensometer on the flange of a stiffened plate girder. A maximum tensile stress of 7100 psi was recorded during welding and a final compression after cooling of 1210 psi, using interrupted fillet welds, 0.24-inch leg. Continuous fillet welds produced a final compression of 7400 psi and uninterrupted fillets made by the back-step procedure produced 7800 psi final compression. Using the X-ray method, Möller[44] found tensile stresses of 43,000 to 72,000 psi on a welded, ribbed flange girder (no details). Wever and Rose[45] also applied the X-ray method to a welded ribbed flange I beam. The beam was 20 x $12^1/_2$ inches, web thickness 0.59 inch, flange thickness 0.79 inch, mild steel. The web was welded to the flanges by continuous fillets (no details). High residual compressive stress up to the yield point was found everywhere on the surface of the beam. The residual stress was due as much to rolling as to welding, and disappeared completely on etching the surface with acid to a depth of 0.02 inch.

Residual stresses induced in the 85-ft. columns of a tower bridge upon being reinforced by arc welding, Fig. 28, were measured by Wilson and Brown,[46] using Berry gages (8-inch gage length). The deformations due to welding on gage lengths remote from welds were measured and converted directly to stress by means of Young's Modulus. The average residual compressive stress at several points in several columns was 6480 to 11,400 psi.

A few measurements of apparent shrinkage stresses in welded wrought iron plate girders and in riveted girders

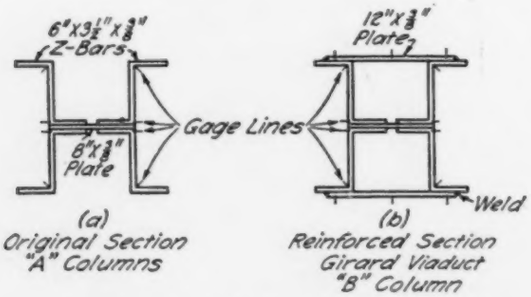


Fig. 28—Sections of Columns Tested by Wilson and Brown[46]

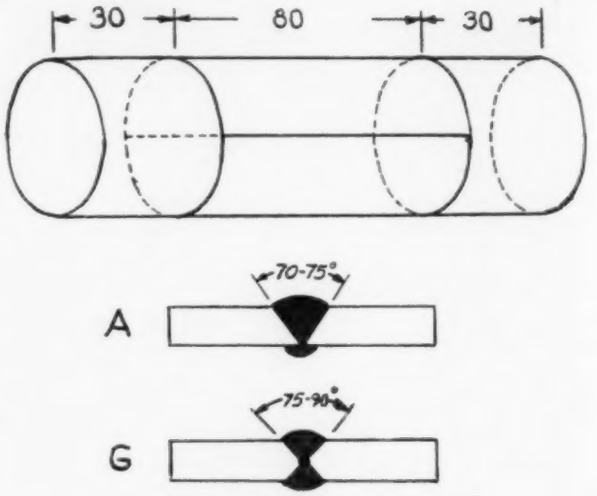


Fig. 29—Experimental Drums Tested by Bollenrath[51]
A—Type of arc-welded joint, 0.08-inch shoulder; 5 layers plus 1 or 2 reverse layers
G—Type of oxyacetylene welded joint; 0.12-inch shoulder; 1 layer on each side.
Plate is 0.59 inch thick. Drum is 67 inches outside diameter.

stiffened by welding are given by Ellson.[47] Continuous welding gave the same results as stitch welding. In general a reinforcing plate welded to the web of a riveted girder induces tensile stress (15,000 psi) in the added plate and compression (4500 to 9000 psi) in the original plate. Extensometer readings by Bruff[48] on plate-girder railway bridges strengthened by arc welding showed that, unless correct welding sequence was adopted, the maximum stress in the strengthened bridge under a given load may exceed that in the unstrengthened bridge.

Welded Lattice Girders

In view of the complicated distribution of reaction and local residual stresses that may be expected in welded lattice girders it is not surprising that only two investigations of this subject have been made. Camoletto[49] measured deformations during welding of a single bent (15 feet long) of a lattice truss ($16^1/_2$ feet high) with diagonal bracing. An acoustic extensometer and a deflectometer (0.0004 inch accuracy) were used to measure strains in the longitudinal, transverse and diagonal directions along the central axis of the compression of flanges. A compressive stress of 14,000 psi was found in the central axis of the stiffening plate as a result of welding. The principal stresses were calculated to be −29,000 and 40,000 psi in the center of the cover plate of the top member. The stresses at the edges of the plate due to welding were entirely tensile. Measurements at 24 points on the diagonal brace revealed a maximum stress near the center of 19,000 psi tension in all directions.

Okerblom[50] made strain gage measurements on an arc-welded box lattice girder (incomplete details). The apparent maximum residual stress was 5000 psi compression.

Welded Boilers and Other Cylindrical Vessels

Bollenrath[51] used the Mathar method to measure residual stresses in oxyacetylene and arc-welded drums, Fig. 29. The normalized boiler plate was 0.59 inch thick and had the properties shown in Table 26.

The diameters of the electrodes were 0.13 (135 to 160 amp.), 0.16 (200 amp.) or 0.20 inch (260 amp.), most of the welding being done with the 0.16-inch electrodes.

Table 26—Properties of Boiler Plate Used by Bollenrath[51]

	Boiler Plate In Direction of Rolling	Boiler Plate Transverse to Direction of Rolling	Covered Electrode All-Weld-Metal
Tensile strength, psi	60,000	59,000	75,500
Yield point, psi	about 47,000	about 47,000	.....
Proportional limit, psi	32,800	35,400	61,000
Elongation, % in 10 D	26.3	28.8	14.4
Young's Modulus, psi	29,500,000	29,200,000	28,500,000

Arc voltage was 31. Oxyacetylene welds were made by the two-torch vertical method, a length of $3^1/_4$ to 4 inches being welded, then peened at red heat with an air hammer, and torch annealed. Welding velocity was 24 to 28 inches per hour. Oxygen consumption was 26.5 cu. ft. per hour. The usual burner for 0.24 to 0.35-inch plate was used. The two-torch vertical method of oxyacetylene welding is said to produce $^1/_2$ to $^1/_3$ the residual stress of continuous gas welding; peening still further reduces the stress. The exact sequence of welding the seams is not described, but the longitudinal welds were made, then the drums were butt welded together and the ends attached. The Mathar holes were made in plate metal alongside the weld because the surface of the weld was rough. Eighteen holes at various points were made along and between the two middle circumferential seams.

In the *arc welded drum* maximum residual tangential stresses of 43,000 to 50,000 psi were found along the circumferential weld near the middle of the drum. The accompanying axial stress was 35,000 psi compression. The maximum axial tensile stress near the longitudinal weld was 67,000 psi, the accompanying tangential stress being ±30,000 psi. At the junction of longtitudinal and circumferential weld the tangential tensile stress rose in one instance to 50,000 psi; the accompanying axial stress was −28,500 psi. The residual stresses were maximum along the welds, the stresses in either direction at a distance from the weld being ±15,000 to ±21,000 psi maximum. The stresses were generally higher in the longitudinal seams than in the circumferential. A rise in yield point of 12 to 35% was indicated due to bi-axial stress in the weld.

In the *oxyacetylene welded drum* the highest stress observed was 32,000 psi compression in the circumferential welds. The maximum tensile stress along the circumferential seams was 17,000 psi; the average stress was 11,000 to 13,000 psi compression. In the longitudinal seams the maximum tension was 28,500 psi; the maximum compression was 14,000 psi.

Ebel and Reinhard[52] oxyacetylene welded two sets of two drums 16 inches long, 30 inches diameter, with longitudinal X welds in 0.47-inch boiler plate having a tensile strength of 50,000 to 63,000 psi, elongation 22 to 27%. After the longitudinal seams were welded the drums were normalized at 930° C. Each set of drums was then welded together with a circumferential X weld. One drum was torch annealed over a zone 6 inches wide across the circumferential seam by moving two torches forward at a uniform rate. The temperature was about 930° C., time 60 minutes. The circumferential seam of the other drum was annealed by a circular gas burner, the drum being vertical and being rotated to assure uniform heating. A temperature of 930° C. was maintained for 25 minutes. The upper end of the drum reached 760 to 800° C., the lower end 800 to 850° C. During cooling, when the circumferential seam and adjacent zone 12 inches wide were at about 600° C., the ends were black. Therefore, it was believed that the heat treatment corresponded to a stress-relief anneal at 600° C., the heating at 930° C. probably having no effect on residual stress, but having some effect on microstructure.

Extensometer measurements (0.79-inch gage length, accuracy 2800 psi) were made on specimens cut from the drum. The measurements were made on gage lengths parallel and perpendicular to the circumferential seam for a distance of 8 inches on each side of the seam and perpendicular thereto at intervals of 120° around the drum. The specimens for the ring-annealed drum had a width of 16 inches on each side of the seam instead of 8 inches. Residual stresses were calculated directly from the strain gage reading, using Young's Modulus.

In the torch-annealed drum the maximum axial stresses on the outside of the drum were +23,000 psi and −27,000 psi; the maximum circumferential stresses were +28,500 psi and −18,500 psi. The distribution of stresses at the three sections was not similar. The residual stresses in the ring-annealed drum were small, the welded region being practically free from stress.

The residual stresses in a boiler drum welded with austenitic electrodes (25 Cr, 20 Ni) were measured by Kautz[53] after the drum had undergone a hydraulic fatigue test (no details). The drum was 32 inches outside diameter, 29 inches long, not including elliptical ends, had three circumferential and one longitudinal weld and was made from 1.10 inch mild steel (0.25 C, 0.55 Mn, 0.05 Si, yield point 43,500 psi, tensile strength 71,000 psi, elongation 23 to 25% in 10 diam. reduction of area 43 to 48%). A ring about 1 inch wide was lathe turned from the drum and strain gage readings were taken on an orthogonal mesh ruled on the drum. Residual stresses were computed by means of Young's Modulus from changes in strain gage readings after cutting. The accuracy was ±1400 psi.

The maximum axial stress was +17,000 psi (+15,700 psi at the junction of longitudinal and circumferential seams). A maximum tangential stress of only +5700 psi was observed. The sum of maximum residual stress and stress applied in the fatigue test was about equivalent to the yield point of the plate.

A comprehensive research on residual stress in welded cylindrical vessels has been carried out by Paton, Gorbunov, Bershtein, and Dsevaltovsky,[14] who used light-coated 0.16-inch electrodes and 0.24-inch mild steel plate. All welds were 80° V, 0.12-inch root spacing, 0.06-inch shoulder.

To determine the residual circumferential stress due to welding a circumferential seam between two longitudinally welded and annealed drums 18 inches long, 15 inches outside diameter, sixteen gage holes were drilled at equal intervals on the circumference of the drum both inside and outside. Six sets of 16 holes were drilled in each drum. The first set was 0.39 inch from the shoulder; the five remaining sets were spaced at 0.63-inch axial intervals. The drums were again annealed and welded. The welds were made in light sections of 45° each. Each section was begun 90° or more from the end of the preceding section. Measurements of internal and external diameter were made with dial-gage trammels on each of the eight diameters marked on each of the six rings in both drums after welding and after the rings were cut from the welded drums by means of a lathe. The circumferential (hoop) stress was calculated by

$$S_c = -\frac{\Delta D}{D} \times E$$

where D = drum diameter
ΔD = difference between diameter of drum after welding and after cutting
E = Young's Modulus

The circumferential stress, averaged on the eight diameters of each ring, was +16,000 psi in the weld, was zero at a distance of $^5/_8$ inch from the weld and reached a maximum compression of 19,000 psi about $1^1/_4$ inches from the weld. The compressive stress gradually decreased to nearly zero again at about 3 inches from the weld.

Residual axial stresses were measured in a similar drum with welded circumferential seam. Gage lengths were drawn to span the weld both inside and outside at 24 equally spaced sections along the weld. A dial extensometer with a gage length of 10 inches was used. The drum was cut into axial strips, the gage length being in the center of each strip. Axial stresses were determined by the formula

$$S_a = \frac{\Delta}{10} \times E$$

where Δ is the change in gage length caused by cutting.

There were seven regions of tensile residual axial stress and seven of compression fairly uniformly distributed about the circumference. The maximum tensile stress was 17,200 psi; the maximum compressive stress was 14,200 psi.

The residual axial stress in drums 36 ft. long, 15 inches diameter made with the same materials as the preceding drums was measured on 25 axial gage lengths each 10 inches long located in the middle of each of three equal sections into which the total length of the drum was divided. The weld was step-back welded in five steps starting at the middle of the drum. After the drum was welded each gage length was cut out by means of a milling cutter.

The weld and its vicinity for a distance of about $1^1/_4$ inches on each side were in tension, the maximum being 32,000 psi in the weld. Beyond the tension zone there was a zone of compression about $2^1/_2$ inches wide, the maximum compression being 9700 psi. Elsewhere the residual axial stress did not exceed 4000 psi.

The circumferential residual stresses due to a longitudinal weld were measured in a drum of the same kind used for measuring axial stresses. Nine circumferential gage lines were drawn at about 2-inch intervals starting with the first ring $^1/_2$ inch from the end of the drum. Each gage line had 12 equally spaced holes on the inside and 12 holes on the outside of the drum. After the drum was welded each ring was turned from the drum by means of a lathe. Measurements of changes in inside and outside diameter were made in the same way as for the drum with a circumferential weld.

The end of the weld, which contained the last step of welding was in compression. The maximum residual circumferential stress occurred at the very end of the drum and was 10,000 psi. At a distance of 12 inches from this end the compression changed to a tension which reached a maximum of 4500 psi, beyond which the measurements did not extend.

Paton and coworkers also measured the residual stresses in a welded drum 15 inches diameter, 16 inches long, to which a flat end had been fillet welded. Rings were turned from the drum and the end disk after welding, the fillet weld being made in eight sections, 0.32-inch legs. The residual circumferential stress was calculated by means of the formula given above. The radial and tangential stresses S_r and S_t, respectively, were calculated by means of the formulas

$$S_r = -\frac{E}{1-u}\, e_r$$

$$S_t = -\frac{E}{1-u}\left(e_r + R\frac{de_r}{dR}\right)$$

where u = Poisson's ratio
e_r = unit radial change
R = radius of section under consideration.

The radial and tangential residual stresses in the end before and after the drum had been cut away are shown in Table 27. The maximum residual circumferential stress in the drum occurred at the welded end and was +33,000 psi. At a distance of about 1 inch from the end the circumferential stress changed to compression, the maximum being −7500 psi.

Table 27—Residual Stresses in a Flat Plate Welded on a Drum. Paton and Coworkers[14]

	Radial Stress, psi		Tangential Stress, psi	
	Before Removing Drum	After Removing Drum	Before Removing Drum	After Removing Drum
5.5 inches from center	−11,500	−9,000	−11,500	− 8,700
At the edge	0	0	+46,000	+48,000

Fundamentally the same character of results was obtained for the residual stresses set up on fillet welding a nozzle 6 inches diameter, 16 inches inside diameter. The weld was made in three sections. The maximum tangential stress in the plate (+40,000 psi) occurred at the welded edge. The tangential residual stress at the outer edge was 5500 psi compression. The maximum circumferential stress in the nozzle (+25,000 psi) occurred at the weld.

All in all, the results of Paton and coworkers may be summarized in Table 28. Extremely high tensile residual stresses (over 40,000 psi) were found in the vicinity of fillet welds connecting flat plates to hollow cylinders.

Table 28—Maximum Residual Stresses Measured by Paton and Coworkers[14]

Seam	Type of Stress	Maximum Tension, psi	Maximum Compression, psi
Circumferential	Circumferential	16,000	19,000
"	Axial	17,200	14,200
Longitudinal	Circumferential	4,500	10,000
"	Axial	32,000	9,700

Nikolaev[54] has measured the residual stress perpendicular to a longitudinal arc-welded seam in mild steel drums 40 inches long, 36 inches diameter, 0.47-inch wall, using the methods described in the section on Arc Welds Without Restraint. The welds were single V made in two layers with 0.16-inch heavy covered electrodes (200–220 amp.) for the first layer and 0.20 inch heavy covered electrodes (260–280 amp.) for the second layer. The results are shown in Table 29. The maximum tensile stresses occurred on the gage lengths nearest the seam. ($1^5/_8$ inches from center line of weld), but were not uniformly or regularly distributed along the length of the weld.

An indication of the tangential stress in drums with longitudinal seams welded with bare electrodes, or by the carbon-arc process was obtained by Sandelowsky[31] by cutting the welded drums into 4-inch lengths and slitting

Table 29—Residual Stress in Arc-Welded Drums. Nikolaev[54]

Method of Welding	Maximum Tensile Stress, psi After First Layer	Maximum Tensile Stress, psi After Both Layers	Maximum Compressive Stress, psi After First Layer	Maximum Compressive Stress, psi After Both Layers
Two continuous layers in the same direction	16,000	17,500	5,700	10,700
Step-Back welding, seven steps, opposite directions	12,400	50,000	12,800	5,700

these with a longitudinal saw cut. The amount these slits sprang apart or closed together indicated the kind and magnitude of the residual stresses. The drums were 40 inches long, 18 inches inside diameter, 0.47-inch wall, with reinforcing straps $2^3/_8$ x 1 x 0.32 inch. Without straps the compression at the beginning of the seam was 0.24 inch, increasing to 0.63 inch two-thirds of the distance toward the end, and decreasing to 0.35 inch at the end. With straps on the outside of the seam the slits tended to open (tensile stress) a maximum of 0.47 inch.

A determination of the sum of the principal stresses due to welding Cr-Mo and plain-carbon aircraft tubing (0.04-inch wall) has been made by Bollenrath and Cornelius.[55] The X-ray method was used (no details) and their results are regarded as tentative. The sum of the principal stresses is given in Table 30.

Table 30—Residual Stress in Welded Aircraft Tubing. Bollenrath and Cornelius[55]

Kind of Tubing	Sum of Principal Stresses, psi in Weld	Sum of Principal Stresses, psi in Heat-Affected Zone
Plain carbon	−11,400 to +51,000	−38,500 to +68,500
Chromium-Molybdenum	−38,500 to + 8,500	−30,000 to + 5,700

Residual stresses caused by welding boiler stays have been studied by Strutskov[56] and Müller.[57] Strutskov duplicated conditions in welding stays in a marine boiler by welding 16 equally spaced stays in 4 rows between two mild steel plates containing 0.15 C, 0.55 Mn, 0.06 Si. The plates were $^1/_2$ inch thick and were welded on two opposite sides to steel slabs 40 inches square, 18 inches thick. The bars were $^3/_4$ inch diameter and were welded in a sequence designed to minimize residual stresses. The welding was done with 0.16-inch chalk-coated electrodes. The bulging of the plates was measured as well as the apparent residual stresses on five-gage lengths along the center line of the plate (Huggenberg extensometers, 0.79-inch gage length). Results are given in Table 31, the stress being measured parallel to the line joining stays and being computed directly by use of Young's Modulus.

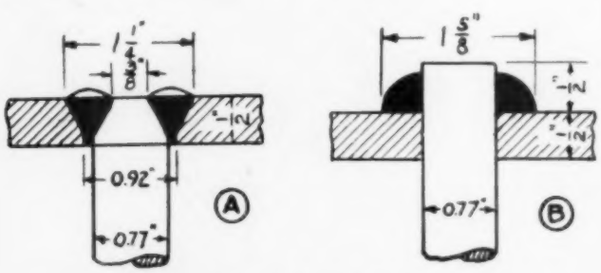


Fig. 30—(A)—Method of Welding Boiler Stays with V Welds
Shoulder = 0.08 inch. Reinforcement = 0.08 inch. Strutskov[56]
Fig. 30—(B)—Method of Welding Boiler Stays with Fillet Welds. Strutskov[56]

Table 31—Apparent Residual Stress in Boiler Plate Caused by Arc-Welding Boiler Stays. Strutskov[56]

Type of Joint	Maximum Bulge, Inch	Maximum Residual Tensile Stress, psi
Fig. 30 (*a*)	0.040	25,000
Fig. 30 (*b*)	0.055	37,000

Müller[57] made a somewhat similar study, without, however, measuring residual stresses. The stays were rectangular rods 12 inches long, $2^3/_4$ x 0.67 inch cross section. In arc welding seven of these stays into two boiler plates (no details), Müller found that the welds cracked if one support was allowed to cool before the next was welded. Müller also discovered that no cracks occurred if the supports were water cooled during welding.

Cracks having developed in the welded shell plates of a barge constructed of a rather high-tensile steel, Bennett[125] drilled small disks from the plates at five locations, which, unfortunately, were not very close to the weld. The disks had previously been punch marked and accurately measured with a Berry strain gage. The results showed some readings of the order of 25,000 psi (compression or tension). Bennett estimated that the residual stresses in the immediate vicinity of the weld may have been perhaps greater than twice the stress observed in the disks.

Beads Deposited on Surface

The residual stresses set up in steel with one or more beads of weld metal deposited on the surface has been investigated usually in an attempt to separate effects due to the weld metal itself and effects due to a joint. There seems to be some fundamental difference in the character of the residual stresses between beads deposited on the edge of a relatively thin plate and beads deposited on a surface perpendicular to the thickness. For this reason, investigations on beads deposited on the edge of a plate are dealt with in the section following this.

The Sachs Method has been used by Bühler and Lohmann[58] to study residual stresses in disks having concentric beads deposited on the surface. The method of measurement and materials are described in the sections on Oxyacetylene and Arc Welds with Restraint. Soft steel (St 34) 0.79 inch thick was used. The first series of tests consisted of depositing beads of covered electrode weld metal on both sides of a disk 9.7 inches diameter. The beads were deposited on a circle at a different distance from the center in each disk.

The results are shown in Table 32. The bead at 0.0 inch was simply a lump of weld metal deposited in the center of the disk on both sides. The maximum stresses were tensile. The residual stresses depend on the distance of the bead from the edge of the disk. The two factors at work are difference in temperature and size and location of bead. The smaller the diameter of the bead the lower is the heat input, but the larger is the ratio of volume of elastic to inelastic metal during welding. The same character of stress distribution was obtained with bare as with covered electrodes.

Another series of tests was made further to investigate the "size" effect. Weld metal was deposited in two types of grooves, Fig. 31, cut on both sides of the disks. Table 33 shows that the maximum residual stresses depend, as in the preceding experiments, on the size of disk and bead as well as on the heat imput. The stress distribution

was also entirely different for disks of different diameters, as shown in Fig. 32. In the large disks oxyacetylene welding, with its high heat input, was at a slight disadvantage, whereas in the small disks, oxyacetylene welding tended to heat both the center and the edges so that low residual stresses were created. The broad groove usually created higher residual stresses than the narrow goove.

Table 32—Maximum Residual Stresses in Disks with Beads Deposited on Both Surfaces. Bühler and Lohmann[58]

Diameter of Bead, Inch	Maximum Tangential Stress, psi	Maximum Radial Stress, psi
0.0	29,000	29,000
2.0	58,500	58,500
3.2	67,000	24,000
5.3	33,000	11,000
7.9	27,000	17,000

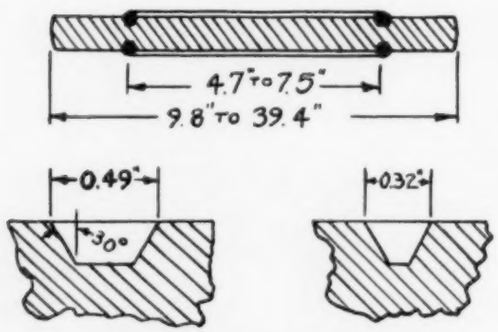


Fig. 31—Disks and Grooves for Weld Metal
Grooves are 0.20 inch deep. Disk is 0.79 inch thick. Bühler and Lohmann[58]

The residual stresses in a shaft built up by surface welding have been measured by Ehrt and Kühnelt.[59] The shaft was annealed 0.6% C steel having a tensile strength of 102,000 psi. It was 24 inches long, $4^1/_2$ inches diameter. The circumference of the shaft was coated with single axial beads of the electrodes shown in Table 34. The coating of electrode No. 4 contained 1% C, 24% Mn. The aggregate carbon content was estimated to be 0.12%. All electrodes were 0.16 inch diameter, 130 amp. As soon as the shaft became sufficiently hot to show temper colors, it was allowed to cool before further welding was done; after being surfaced the shaft was turned down to 4.60 inches diameter and was cut into 4-inch lengths. Length and diameter of these sections were measured to ±0.0002 inch. The sections were then drilled out to 0.18-inch wall thickness and again measured. Measurements were repeated after the hollow cylinders were slit axially.

The stresses shown in Table 34 were calculated by means of Kirchberg's method. The stresses designated "outside" refer to the surface of the turned shaft; those designated "inside" refer to the inside surface of the 0.18-inch tube corresponding to the hardened zone. It is in this zone that the maximum residual stresses occur. The residual stresses decrease as the carbon content of the electrodes approaches that of the shaft, possibly on account of the effect of carbon on the coefficient of thermal expansion.

The residual stresses in mild steel tensile specimens 0.59 inch thick, Fig. 33, with a bead on one or both sides deposited by heavy covered mild steel electrodes, have been measured by two methods by Thum, Kaufmann and Schönrock.[60] In Method No. 1 cuts were made along *A-A*, then along *B-B*, and finally along *C-C*. Results from the *C-C* cuts were unreliable because the thin strips were deformed by sawing. In Method No. 2 cuts *A-A* were followed by cut *B-B*. Measurements on the gage lengths shown in Fig. 33 were made before and after

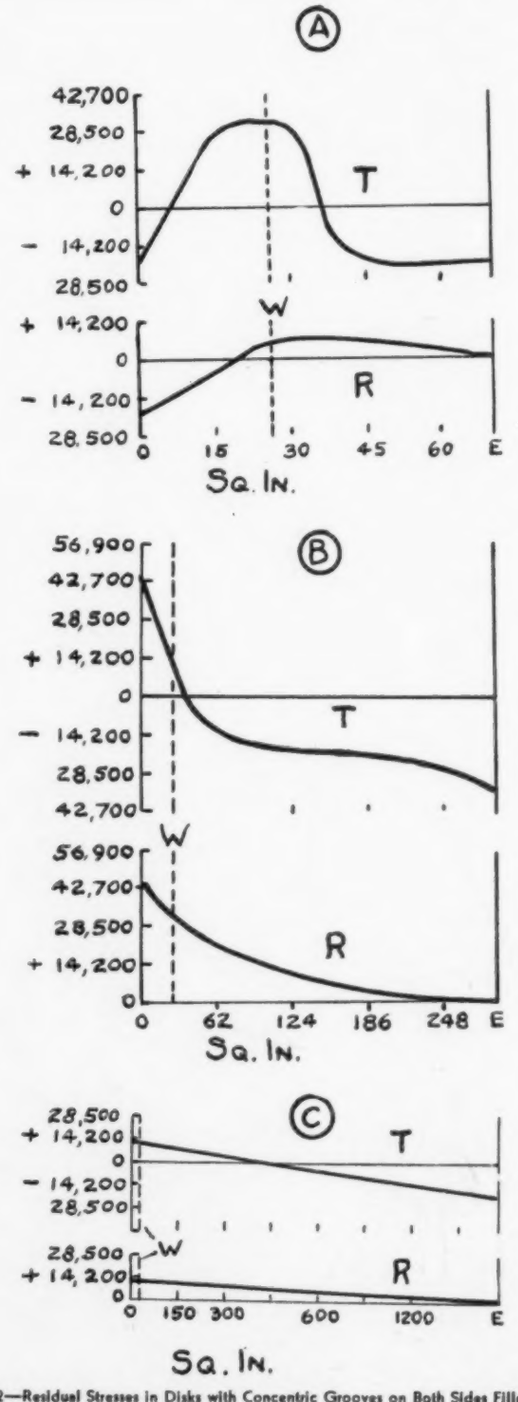


Fig. 32—Residual Stresses in Disks with Concentric Grooves on Both Sides Filled with Weld Metal. Covered Electrodes. Broad Groove, 6 Inches Diameter
T = tangential residual stress
R = radial residual stress

E_1 edge of plate $\frac{\pi}{4}(9.7)^2 = 7.5$ sq. in. for disk 9.7 inches diameter

Abscissas are surface area of disk within the section under consideration
(a) Disk 9.7 inches dimater
(b) Disk 19.7 inches diameter
(c) Disk 39.4 inches diameter. Bühler and Lohmann[58]

Table 33—Effect of Size of Bead and Disk on Residual Stresses. Bühler and Lohmann[58]

Diameter of Disk, Inches	Diameter of Groove, Inches	Maximum Stress Component, psi	Arc Welding		Oxyacetylene Welding
			Bare Electrodes	Covered Electrodes	
		Narrow Groove			
9.8	4.7 and 7.5	tangential	+43,000	+33,000	+ 4,300
		radial	+24,000	+33,000	+ 2,800
19.7	4.7	tangential	+28,500	+25,500	+30,000
		radial	+28,500	+25,500	+30,000
19.7	7.5	tangential	+24,000	+35,500	+25,500
		radial	+20,000	+33,000	+20,000
		Broad Groove			
9.7	5.9	tangential	+28,500	+31,000	+ 8,500
		radial	−20,000	−20,000	+ 4,300
19.7	5.9	tangential	+53,000	+47,000	+56,000
		radial	+53,000	+47,000	+56,000
39.4	5.9	tangential	−25,500	−21,500	−40,000
		radial	+17,000	+10,000	+28,500

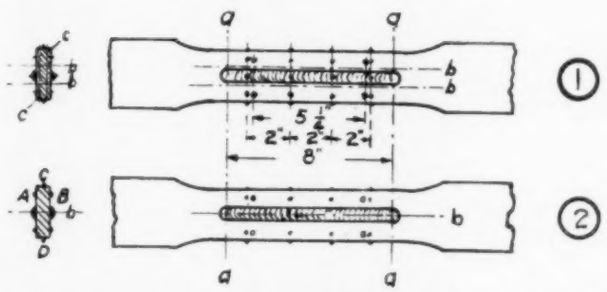


Fig. 33—Two Methods of Measuring Residual Stresses in a Specimen with Bead Deposited on One or Both Sides

⊙ extensometer
O deflectometer
—·—·— = saw cuts
Thum, Kaufmann and Schönrock[60]

each cut, the corresponding stress in the direction of the bead being calculated by means of Young's Modulus. Thum and coworkers only report average results over the cross section. The variation of residual stress along the length of the bead is not stated, but the results plotted in Fig. 34 are averages of all sections by both methods of measurement. The stresses measured by Method No. 2 need not necessarily plot uniformly with the results of Method No. 1. However, the symmetry of the specimen is such that both methods are in fair agreement. The average residual stresses determined on the sides of the specimens are plotted but cannot be regarded as the end point of any of the curves. The results show that straightening (no details) reduces the residual stresses, but that beads on both sides create higher residual stresses than beads on one side, especially if the second bead is deposited on a cold specimen. The maximum tensile residual stress occurs in the immediate vicinity of the bead.

The amount of residual axial stress in the beads themselves was determined by taking extensometer and deflectometer measurements on specimens shown in Fig. 33 before and after milling off the bead. Table 35 shows that the second bead contains about 75% as much residual tensile stress as the first bead. The annealing effect of the second bead on the first in the thick specimens that were used was probably very slight. No reason is given for the differences between the extensometer and deflectometer, which are not described in detail.

The residual stresses acting in a direction parallel to two beads of weld metal about 7 inches long deposited on both sides of a mild steel plate 8 x 20 x 0.39 inches were shown to be tension by Graf,[61] who used the subdivision method but did not obtain quantitative values of stress.

Bollenrath[2] employed the Mathar Method to measure the residual stress in a mild steel plate 24 inches square, 0.59 inch thick on the surface of which a bead of bare wire about 3½ inches long had been deposited by carbon arc. The penetration was 0.08 inch and the specimen was presumably cooled in air after preparation. A maximum tensile stress of 71,000 psi was found in the unwelded surface and a maximum of 107,000 psi in the welded surface.

Table 34—Residual Stresses in a Shaft Built-Up by Surface Welding. Ehrt and Kuhnelt[59]

Electrode No.	C	Mn	Si	S	P	Tangential Tensile Stress, psi		Axial Tensile Stress, psi	
						Outside	Inside	Outside	Inside
1	0.08	0.32	0.01	0.014	0.023	20,300	34,000	4400	34,500
2	0.36	0.24	0.08	0.026	0.018	18,600	24,500	3400	27,200
3	0.70	0.35	0.10	0.015	0.015	11,400	20,000	850	21,700
4	0.09	0.5	0.02	0.006	...	8,700	30,500	1000	28,300

Table 35—Residual Axial Tensile Stresses in Beads. Thum, Kaufmann and Schönrock[60]

Specimen	Residual Axial Stress, psi			
	First Bead		Second Bead	
	Extensometer	Deflectometer	Extensometer	Deflectometer
Bead on one side, as-welded	32,800	24,200	....	....
Bead on one side, straightened	?	0	....	....
Bead on both sides, without intermediate cooling, and in the same direction	20,000	21,400	14,200	18,500
Bead on both sides, with intermediate cooling, and in the same direction	34,200	40,000	21,400	25,700

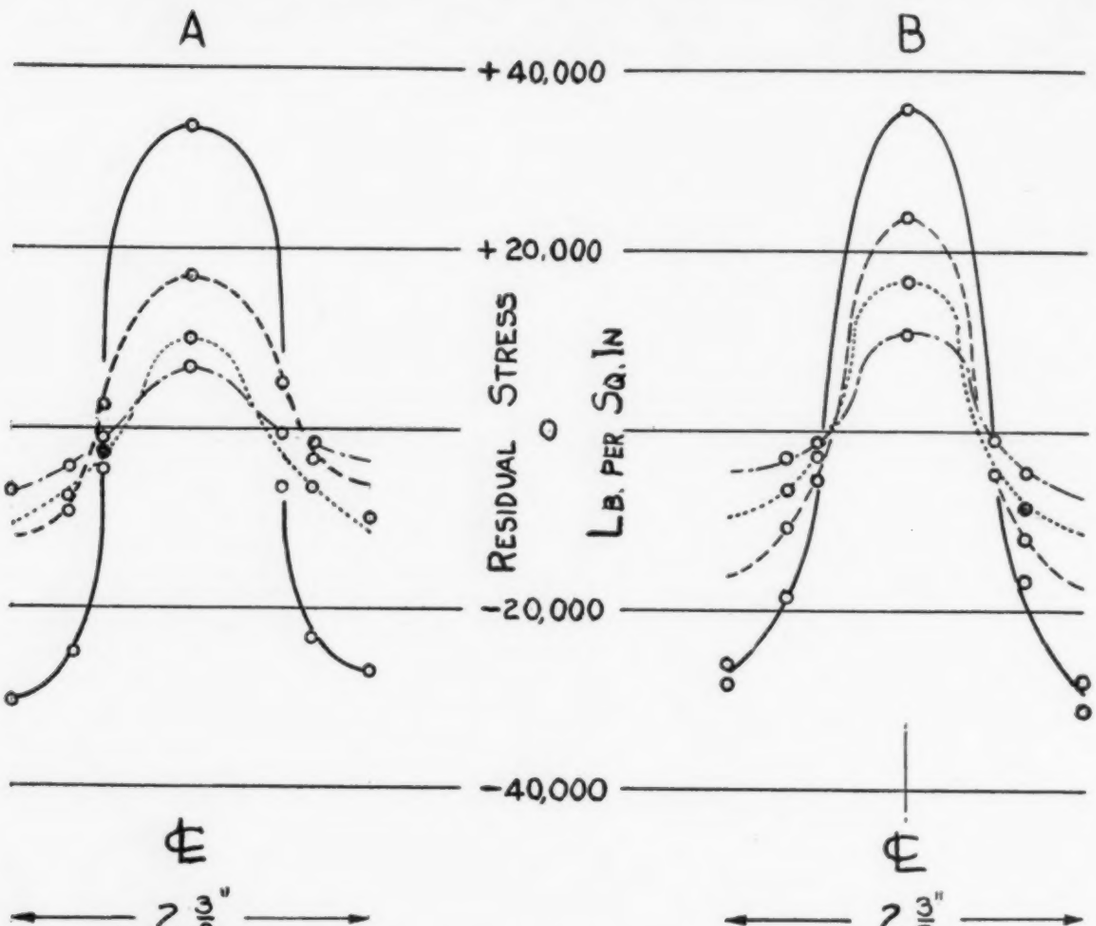


Fig. 34—Average Residual Axial Stresses Across Breadth of Specimens Shown in Fig. 33 Having Beads Deposited on Surface by Arc Welding. Thum, Kaufmann and Schönrock[60]

A. Specimens with bead on one side only
———— Bead on one side, stresses in welded surface
– – – – – Bead on one side, stresses in unwelded surface
. . . . . Bead on one side, subsequently straightened, stresses in welded surface
—•—•— ditto, stresses in unwelded surface.
B. Specimens with beads on both sides
———— Bead on both sides; specimen completely cooled after first bead; second bead deposited in same direction; stresses in surface welded last
– – – – – ditto; stresses in surface welded first
. . . . . Bead on both sides; second bead deposited immediately after the first and in the same direction; stresses in surface welded first
—•—•— ditto; stresses in surface welded last

A maximum tensile residual stress of 34,000 psi (reaction stress) was observed by Lottmann[25] in a rigidly clamped mild steel plate 0.39 inch thick, 80 inches long, 20 inches wide, as a result of depositing five beads of bare electrode weld metal across the width of the plates.

Beads Deposited on Edge of Plate

A plate having beads of weld metal deposited on one or more edges is an interesting subject for residual stress determination because the common unbeveled butt weld may be regarded as composed of two plates with beads deposited on adjacent edges. The subject has been studied primarily by Boulton, Lance Martin and Jamieson. Boulton and Lance Martin[62] investigated the residual stresses parallel to the welded edge or edges, and Lance Martin[18] continued the investigation with different experimental procedure to include residual stresses perpendicular to the weld.

Boulton and Lance Martin[62] used two as-rolled, mild steel plates 9 ft. x 6 ft. x $^3/_8$ inches, Fig. 35, containing 0.18 C, 0.48 Mn. Plate 1 was placed with a long edge uppermost; a bead of weld metal was deposited on this edge at a constant rate in runs of $9^1/_2$ inches, the time required to change electrodes being kept at a minimum. Plate 2 was placed with surfaces horizontal; two welders simultaneously deposited a bead of weld metal along the

Measuring Gauge and Gauge Lines in Plates 1 and 2.

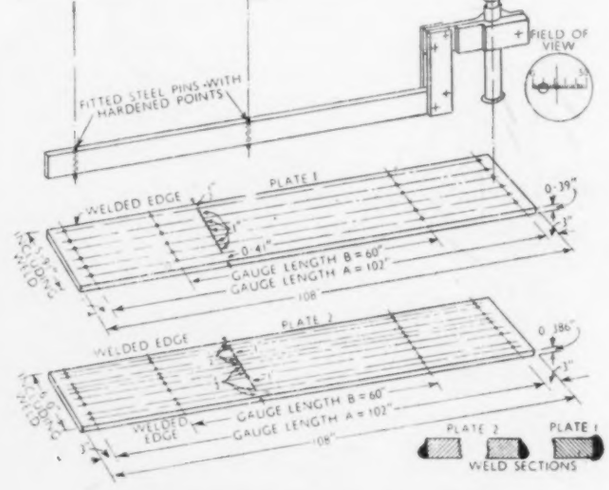


Fig. 35—Specimens Used by Boulton and Lance Martin[62]

9-ft. horizontal edges. The welders always kept opposite each other. Blue-asbestos covered mild steel electrodes (8 gage) were used. Welding details are given in Table 36. Strain was measured on 6 and 9 gage lengths, respectively, on Plates 1 and 2 parallel to the welded edges. The gage lengths were 60 and 102 inches, central with respect to the mid-length of the plate, and were measured with a steel bar gage fitted with a micrometer ocular (sensitivity 0.00039 inch). The gage points were made with some form of punch. Measurements were made on both faces of each plate before and after cutting and in the case of plate 1 after limited subdivision. Two cuts were made on plate 1 after welding. First, a strip 0.1 inch wide containing all the deposited metal was milled off. Then a second strip 1.02 inch wide was cut off. No strips were cut from plate 2. A correction for curvature of plate 1 after welding was necessary, which amounted to the equivalent of 550 and 1400 psi on the short and long gage lengths, respectively.

Plate 1. The difference between gage lengths before and after welding varied linearly with distance from the welded edge, being -0.00137% (contraction) at the welded edge and $+0.00061\%$ (elongation) at the opposite edges. The short and long gage lengths gave practically identical results. Obviously to fulfill the requirements of equilibrium, only the strains over the portion of the plate at a considerable distance from the welded edge can represent residual elastic strains. Boulton and Lance Martin, therefore, propounded a theory to predict the distance from the welded edge at which the measured residual strains represent elastic stress. This theory is based on temperature distribution during welding, and on the elastic properties of steel at elevated temperatures, and is not dependent on the subdivisions, which were made simply as a check. The section on Theoretical Aspects contains a full discussion of the theory. The results of the theoretical calculation are shown in Fig. 36 Curve 2, which also contains the results of subdivision. The welded edge and its vicinity (about 1 inch) at room temperature are stressed to the tensile yield point parallel to the edge. The unwelded edge is also in tension, the intermediate region being in compression to preserve equilibrium.

Theoretical Stress Distributions for Plate 1.

Curve 1 Stress p in plate at instant t_1.
Curve 2 Residual stress f in plate.
Curve 3 Residual stress f' in plate after removing weld metal.
Curve 4 Residual stress f_r in plate after removing second strip.
Curve 5 Residual stress in second strip after removal.

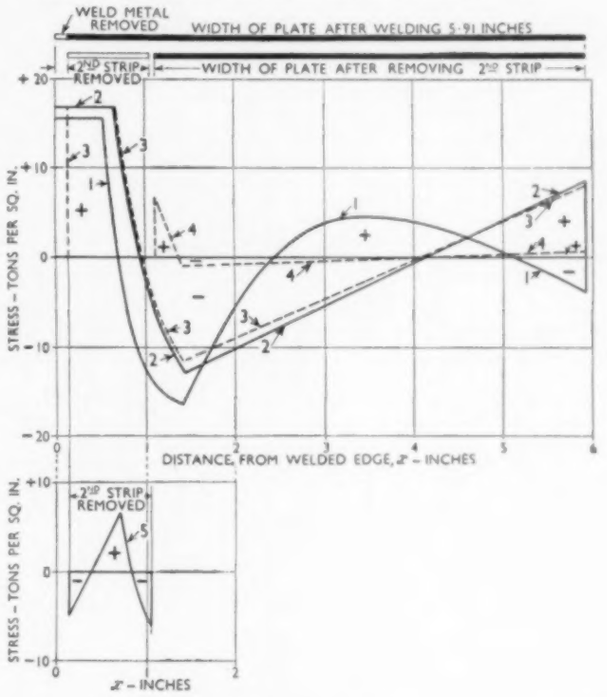

WELD METAL REMOVED
WIDTH OF PLATE AFTER WELDING 5.91 INCHES
2ND STRIP REMOVED
WIDTH OF PLATE AFTER REMOVING 2ND STRIP
STRESS - TONS PER SQ. IN.
DISTANCE FROM WELDED EDGE, x - INCHES
2ND STRIP REMOVED
x - INCHES

Fig. 36—Theory of Inelastic Deformation Applied to Plate 1. Boulton and Lance Martin[62]

Table 36—Welding Details. Boulton and Lance Martin[62]

	Plate 1	Plate 2
Speed of electrode, inches per minute (excluding time to fit new electrode)	3.08	6.22
Number of stops made to fit new electrodes	11	7
Mean duration of each stop, seconds	15.5	32.1
Length of electrode used per inch of run, inches	1.68	1.12
Mean cross section of deposit, sq. in.	0.0337	0.0224
Current, amp.	105	115
Voltage, open circuit, d.c.	50	52

Plate 2. The difference between gage lengths before and after welding was constant across the width of the plate at -0.0006% (contraction). The residual stresses parallel to the welded edges deduced by means of the theory applied to Plate 1 are shown in Fig. 37. Both edges and their vicinity were stressed to the yield point in tension after the specimen had cooled to room temperature. The intermediate region was subjected to a constant residual compressive stress of about 17,000 psi. The change from maximum tension to maximum compression occurred in a distance of slightly less than an inch.

In his later series of experiments which were intended to show the distribution of residual stress parallel as well as perpendicular to welded edges by means of the subdivision method and without recourse to the theory developed in the earlier paper, Lance Martin[18] used a

Table 37—Differences Between Gage Lengths Parallel to the Weld Before and After Welding for Sections Near the Middle of the Length of the Plate. Boulton and Lance Martin[18,62]

Cross Section of Plate, Inches	Length, Inches	Gage Lengths, Inches	Difference, Inch per Inch	Remarks
		Single-Edge Welded Plates		
6 × 3/8 (unannealed)	108	60 x 102	-13.43×10^{-4} to $+5.88 \times 10^{-4}$	Welded edge
				Unwelded edge
4 × 3/8 (annealed)	12	2.013	-21.30×10^{-4} to $+8.19 \times 10^{-4}$	Welded edge
				Unwelded edge
		Double-Edge Welded Plates		
6 × 3/8	108	60 and 102	-6.04×10^{-4}	Unannealed
6 × 3/8	18	2.013	-6.60×10^{-4}	Annealed
4 × 3/8	12	2.013	-12.92×10^{-4}	Annealed

Theoretical Stress Distribution for Plate 2.
Curve 1 Stress p in plate at instant t_1.
Curve 2 Residual stress f in plate.

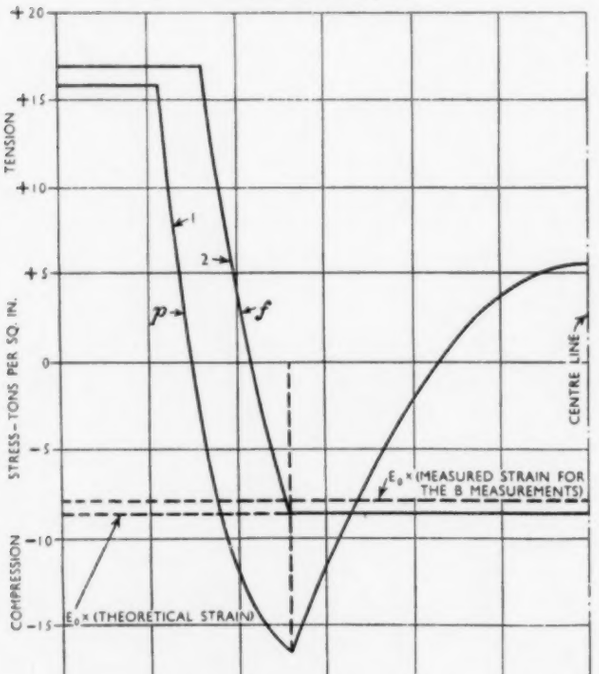


Fig. 37—Theory of Inelastic Deformation Applied to Plate 2. Boulton and Lance Martin[62]

comparator consisting of a microscope with two movable objectives, 2 inches apart and calibrated by means of an interferometer. The gage length was 2 inches and the accuracy was estimated to be $\pm 14.95 \times 10^{-6}$ inch (about ±200 psi). Three specimens, Fig. 38, were prepared from 0.18% C steel (tensile strength 63,000 to 72,000 psi, yield point (unannealed), 40,000 psi, elongation 25% in 8 inches, Young's Modulus 30×10^6 psi) which were annealed at 900° C. after machining. Gage points were made with a lightly loaded diamond. One layer of blue-asbestos covered mild steel electrodes (a.c.) was deposited on the edges at the rate of 4.5 linear inches per minutes. Measurements of the gage lengths were made before and after welding, and after subdivision. The cut strips were also measured after annealing in hydrogen for 1 hr. at 650° C. No information is given about the yield point of annealed plate or the position of the plates during welding. The physical properties of deposited metal were not determined but were said to have been about the same as those reported by Boulton and Lance Martin. The correction for curvature was evaluated but it was found to be negligible (a small fraction of one division of the micrometer scale).

Single-Edge Welded Plate

Dealing first with the results of measurements made before and after welding, Lance Martin found that the difference between gage lengths parallel to the welded edge before and after welding varied linearly across the width of the single-edge welded Plate 1. The single-edge welded plate investigated by Boulton and Lance Martin yielded a similar result although the absolute value of the differences was not the same, Table 37. The differences between gage lengths perpendicular to the weld before and after welding for Plate 1 also varied linearly across that portion of the width of the plate on which measurements were made. The perpendicular difference was zero at the section at which the parallel difference was zero. As the welded edge was approached the distances between the gage lengths perpendicular to the welded edge became greater as a result of welding. This elongation was exactly equal to the corresponding shortening of the corresponding gage length parallel to the edge multiplied by Poisson's ratio. Since the sections under consideration remained elastic throughout the time of welding, the results show that the residual stress perpendicular to the weld at a distance of 2 inches or more from the welded edge was zero.

The residual stresses in the single edge welded Plate 1 determined by subdivision are shown in Fig. 39. The distribution of residual stress bears a resemblance to that calculated by Boulton and Lance Martin.[62] The stresses shown in Fig. 39 were determined on strips which had been annealed at 650° C. before final measurement. The stress annealing changed the magnitude of the residual stress determined before annealing by as much as 6000 psi in some cases. Possibly the large size of the strips cut out may explain the effect of stress annealing. The maximum residual tensile stress parallel to the weld was 45,000 psi at the welded edge. The residual stress perpendicular to the weld was practically zero, which was in agreement with the calculations described in the preceding paragraph. Lance Martin did not attempt to repeat the theoretical calculations of Boulton and Lance Martin for the single or the double-edge welded plates, nor did he discuss the reasons for the lack of agreement between the theoretical residual stress distributions and those determined by the subdivision method.

Double-Edge Welded Plates

Although the more refined measurements made by Lance Martin[18] confirmed the results of Boulton and

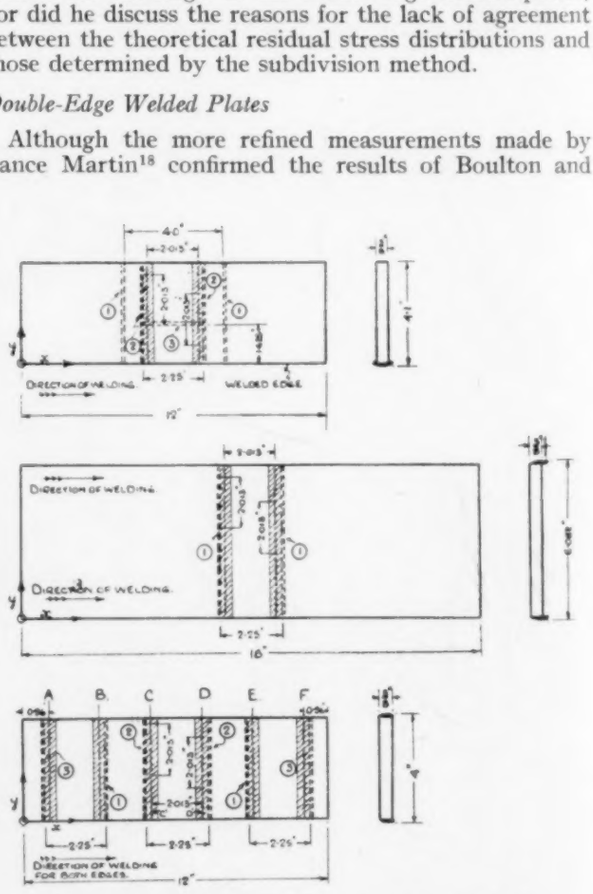


Fig. 38—Edge-Welded Specimens Used by Lance Martin[18]
The surfaces were polished; gage marks are shown shaded. Positions of saw cuts are indicated by doubled dotted lines. The order in which the cuts were made is indicated by numbers in circles

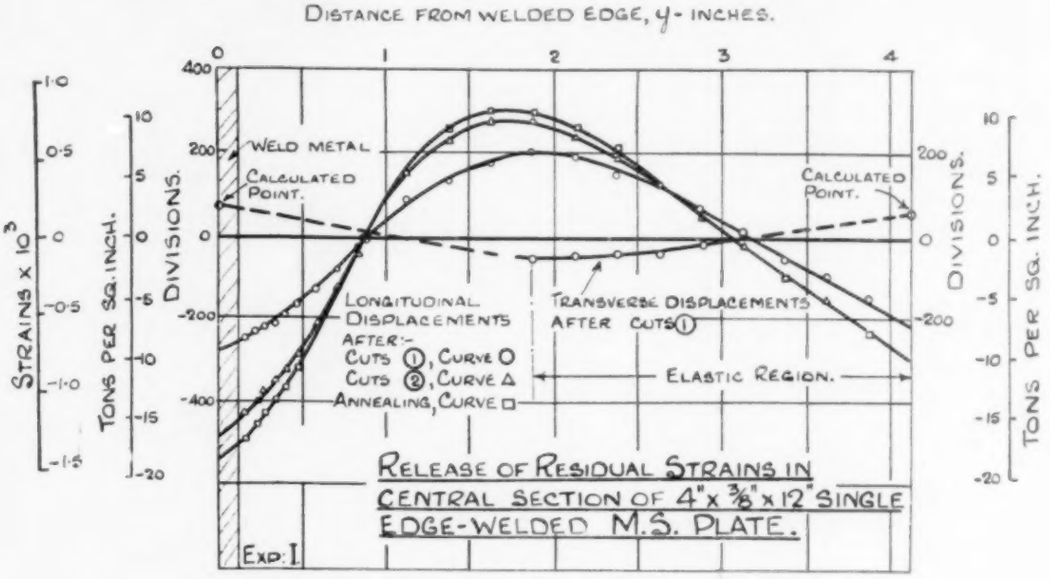

DISTANCE FROM WELDED EDGE, y - INCHES.
STRAINS X 10³
TONS PER SQ. INCH.
DIVISIONS.
WELD METAL
CALCULATED POINT.
CALCULATED POINT.
LONGITUDINAL DISPLACEMENTS AFTER:-
CUTS ①, CURVE O
CUTS ②, CURVE Δ
ANNEALING, CURVE □
TRANSVERSE DISPLACEMENTS AFTER CUTS①
ELASTIC REGION.
RELEASE OF RESIDUAL STRAINS IN CENTRAL SECTION OF 4" x 3/8" x 12" SINGLE EDGE-WELDED M.S. PLATE.
EXP: I.

Fig. 39—Residual Stresses in Single-Edge Welded Plate 1. Lance Martin

Lance Martin[62] for the single edge welded plate, they revealed two striking differences for the double-edge welded plates. In the first place, the differences between the gage lengths parallel to the weld before and after welding did not remain constant across the width of the plate, Fig. 40. In the $^1/_2$-inch zone next to the welded edges the contractions increased greatly as the edge was approached. In the second place, the differences perpendicular to the weld were fairly large and indicated substantial amounts of residual stress perpendicular to the weld.

The residual stresses determined by subdivisions are shown in Fig. 41.

The residual stresses parallel and perpendicular to the welded edges were computed using Young's Modulus and Poisson's Ratio. These stresses are not principal stresses because they were accompanied by shear stress parallel and perpendicular to the weld, except on sections perpendicular to the edge at and near the center of the plate. Sufficient data for computing the magnitudes and directions of the principal stresses were not obtained. The distribution of residual stress parallel to the welded edges is similar in its general features to that predicted by the theory of Boulton and Lance Martin. The maximum residual tensile stress parallel to the welded edges was 56,000 psi at the edges of Plate 2 and 45,000 psi at the edges of Plate 3. The maximum residual tensile stress perpendicular to the weld was 15,000 psi and occurred at the middle of the width of the Plate 3. Residual stresses perpendicular to the welded edges of Plate 2 were not determined. Since Luders lines were present parallel to the direction of the weld runs and in their immediate vicinity. Lance Martin concluded that fairly large shear stresses may exist near the welds. The time during cooling at which the Luders lines appeared was not recorded.

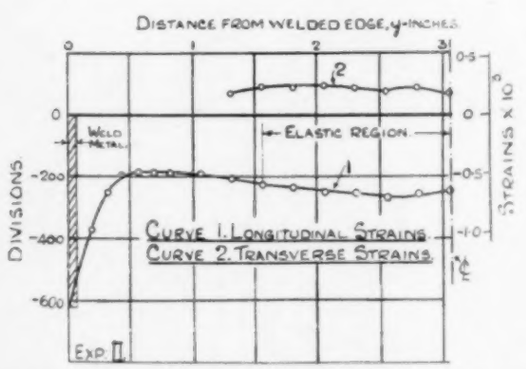


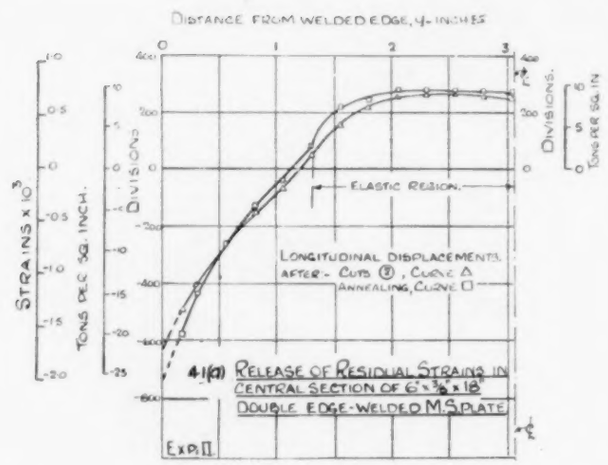
Fig. 40—Residual Stresses in Central Section of Double-Edge Welded Mild Steel Plate 6 x 3/8 x 18 inches. Lance Martin[18]

In considering the results obtained by Lance Martin two facts are of importance. First, the strips cut out were fairly large. Second, the strains parallel and perpendicular to the edges of Plate 3 were by no means uniform at all sections throughout the plate. Furthermore, a small interruption in the welding of Plate 3 created a serious local disturbance of the strain distribution. The regions labeled elastic in the diagrams of stress distribution for Plates 1 and 2 were those in which gage lengths assumed their original value after subdivision and stress annealing. Lance Martin made no attempt to evolve a theory for residual stress perpendicular to the welded edges.

Another thorough investigation of residual stresses in rectangular plates with beads deposited on two opposite edges has been made by Jamieson,[63] whose results are in qualitative agreement with those of Boulton and Lance Martin. He measured the residual stresses parallel to the welded edges by means of the subdivision method. In his first series of tests Jamieson studied the effect on stresses of distance between the welded edges. The specimens were 15 inches long, $^7/_8$ inch thick and 2, 4, 6 and 8 inches wide between welded edges. The edges to be welded were grooved by a V, $^{11}/_{16}$ inch opening, $^3/_8$ inch deep. Bare electrodes were deposited in a single pass in both grooves. The physical properties of weld metal and plate are given in Table 38. Four 3-inch

Table 38—Physical Properties of Weld and Plate Metal Used by Jamieson[63] in His First Series of Experiments

	Yield Point, psi	Tensile Strength, psi	Young's Modulus, psi
Plate metal as-rolled	33,300	60,500	29,100,000
Weld metal bare electrode	36,700	53,500	27,800,000


DISTANCE FROM WELDED EDGE, y-INCHES
STRAINS x 10³
TONS PER SQ. INCH.
DIVISIONS.
TONS PER SQ. IN.
ELASTIC REGION.
LONGITUDINAL DISPLACEMENTS AFTER - CUTS ②, CURVE Δ
ANNEALING, CURVE □
41(a) RELEASE OF RESIDUAL STRAINS IN CENTRAL SECTION OF 6"x⅜"x18" DOUBLE EDGE-WELDED M.S. PLATE
EXP. II

The results are shown in Table 39 The average shortening of the gage lengths recorded in the third column applies to the central portion, omitting the weld strips, and was determined from comparator measurements before and after welding. All cut strips were straight except those containing the weld metal; these bent so that the cut surface was concave. Obviously there was greater residual tensile stress along the cut surface than on the outer surface of the weld. Computation showed that the correction required for bowing raised the maximum tensile and compressive stress in the 6-inch specimen to 16,500 and 12,000 psi, respectively. The correction does not seem to have been applied to any other of the specimens.

Jamieson found by additional experiments (incomplete details) that residual stresses perpendicular to the weld were present in his specimens that were in some cases about one-half as large as the stresses parallel to the weld.

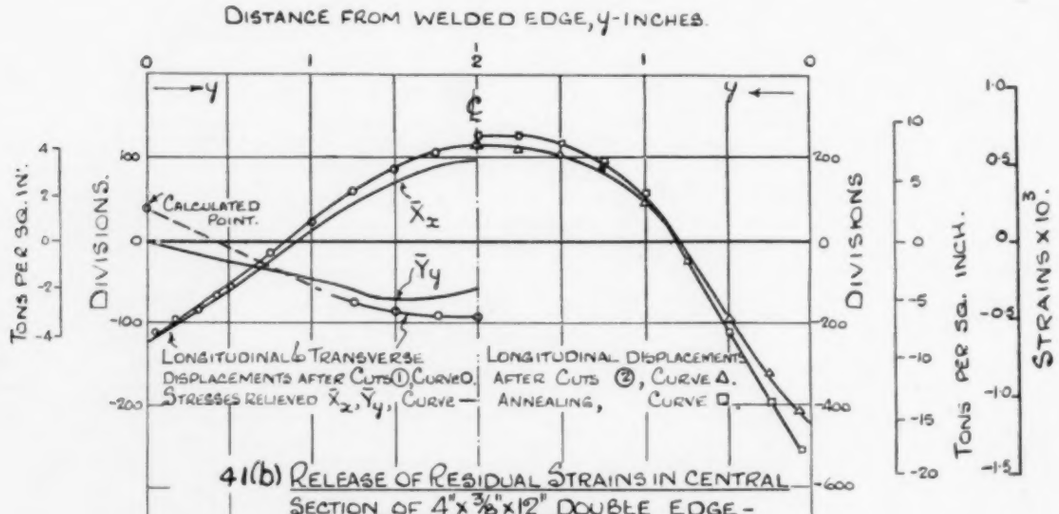

DISTANCE FROM WELDED EDGE, y-INCHES
TONS PER SQ. IN.
DIVISIONS.
CALCULATED POINT.
$\bar{X}_x$
$\bar{Y}_y$
LONGITUDINAL & TRANSVERSE DISPLACEMENTS AFTER CUTS ① CURVE O. STRESSES RELIEVED $\bar{X}_x$, $\bar{Y}_y$, CURVE —
LONGITUDINAL DISPLACEMENTS AFTER CUTS ②, CURVE Δ. ANNEALING, CURVE □
TONS PER SQ. INCH.
STRAINS x 10³
41(b) RELEASE OF RESIDUAL STRAINS IN CENTRAL SECTION OF 4"x⅜"x12" DOUBLE EDGE-WELDED M.S. PLATE.
EXP. III

Fig. 41 (*a*) and (*b*)—Residual Stresses in Double-Edge Welded Plate. Lance Martin[18]

gage lengths were drawn parallel to the edges. There was 1 set of such gage lines on the 2-inch specimen, 3 on the 4-inch, 7 on the 6-inch, 9 on the 8-inch. The gage lengths were measured by means of a comparator before and after welding, and after subdivision into strips 1 inch wide. The edge strips containing the weld metal were $^1/_2$ inch wide. The subdivision was accomplished by saw cuts parallel to the welded edges. The overall accuracy is estimated at ±300 psi.

In another series of measurements Jamieson studied the effect of different welding conditions, Table 40. The specimens were 12 inches wide, 21 inches long (welded edges), and $^3/_4$ inch thick. The grooves were U-shaped $^3/_8$ inch deep, $^1/_2$ inch wide parallel to the direction of welding. Beads were applied in back-step procedure in four equal steps. The electrodes were $^3/_{16}$ inch diameter, 62 volts, open circuit. Copper chill strips were required with the covered electrodes to prevent burning of the

Table 39—Effect of Width of Plate on Residual Stresses in Edge-Welded Plates. Jamieson[63]

Width of Plate Between Welded Edges, Inches	Ratio of Cross-Sectional Area of Plate to Weld	Average Shortening of Gage Lengths in Central Strips, Inch per Inch	Maximum Tensile Stress psi	Location	Maximum Compressive Stress psi	Location	Average Compressive Stress in Central Portion of Specimen, psi
2	0.00195	0.000555	7,700	At weld	7800	Center of plate	7800
4	0.00089	0.000422	18,300	At weld	9000	Center of plate	5800
6	0.00058	0.000311	11,100	At weld	6100	2 in. from weld	5000
8	0.00043	0.000312	17,200	At weld	6700	2 in. from weld	4900

Table 40—Specimens and Materials Used by Jamieson[63] in His Second Series of Experiments

Specimen	Number of Layers	Type of Electrode	Current, Amp.
1*b*	1	Bare (same as first series)	160
2*b*	2	" " " " "	160
1*c*	1	Covered	175
2*c*	2	"	175

	Plate Metal As-Rolled	All-Weld-Metal (Covered Electrode)
Yield Point, psi	33,300	50,000 to 55,000
Tensile Strength, psi	51,500	.....
Elongation, % in 8 inches	32.3	.....
Reduction of Area, %	67	.....

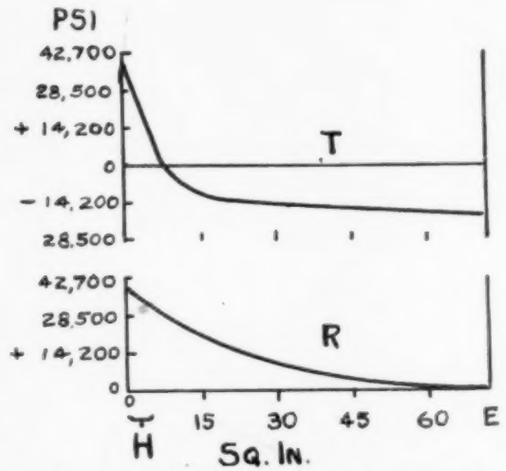


Fig. 42—Distribution of Residual Stresses in a Disk 9.7 Inches Diameter Heated to About 950° C. in a Central Circular Area 2 to 2¾ Inches Diameter by Means of a Welding Torch

H = heated zone, *T* = Tangential residual stress, *R* = radial residual stress, *E* = edge of plate. Abscissa is area of circle concentric with the disk whose radius is the distance from the center of the disk to the point under consideration. Bühler and Lohmann[58]

edges of the groove. Measurements were made with a 10-inch Howard gage on strips 1 inch wide, and were confined to the central 10-inch portion of the specimens to avoid end effects (no details). The ratio of cross-sectional area of weld metal to cross-sectional area of plate was 0.00034.

The results are given in Table 41 and show that, for the particular type of specimen involved, covered electrodes gave higher residual tensile stresses than bare. Two layers created higher stresses (parallel to the welded edges) than a single layer of the same cross section. Jamieson[64] has reported other incidental measurements on similar U-grooved plates (see sections on Stress Relief by Annealing and Stress Relief by Overstrain).

Sandelowsky[55] attempted to measure the residual stress produced by depositing surface layers on the edge of bars 0.63 inch thick by measuring the radius of curvature of the bar as welded. The method is very approximate. Using one coated and five types of bare electrodes he found stresses over 6000 psi tension in only one case. Three continuous layers created about the same radius of curvature (apparent stress) as one layer back-step. Measurements of a similar sort are reported by Ellson[47] and Melcher.[66] As early as 1919 Newton[67] used the subdivision method to determine residual stresses in a disk with a grooved edge. The disk was water cooled during welding. The contraction of the welded ring when it was turned from the disk agreed approximately with the contraction calculated on the basis that the ring was uniformly stressed to the elastic limit (no details).

Flame-Cut Plates

There is no reliable quantitative information on residual stresses caused by flame cutting. Residual stresses were measured by a very approximate method by Wiss[68] in a flame-cut plate 12 x 1½ x 1 inch containing 0.3% C. The residual stress was calculated from the curvature of the plate after 0.24 inch was planed from one of the two cut surfaces (12 x 1½ inches). The results are shown in Table 42. Von Roessler[8] also used an approximate method and observed residual tensile stresses of up to 55,000 psi in flame-cut mild steel. Ruttmann[69] found that the flame-cut surface of mild steel contained residual tensile stress but supplied no details. Siebel and Pfender,[5] and Dehasse[70] measured the deformations produced by flame cutting but their results are not suitable for calculations of shrinkage stresses. An anonymous writer[71] in 1926 stated that in flame cutting large pieces of steel containing over 0.35% C preheating is essential to avoid cracks. Cracks were not encountered in steel containing less than 0.35% C.

Table 42—Approximate Residual Stresses in Flame-Cut Surfaces. Wiss[68]

Thickness of Metal Cut, Inches	Residual Tensile Stress, psi
0.39	1300
1.18	2100
2.36	3100

Local Heating by Torch

Using the Sachs Method described in the section on Oxyacetylene Welds with Restraint, Bühler and Lohmann[58] measured the radial and tangential residual stresses in a mild steel disk 0.79 inch thick, 3.8 inches diameter. The disk was heated to 950° C. in a central circular area 2 to 2¾ inches diameter by means of a torch and cooled in air. The residual radial and tangential stress at the center was +40,000 psi (tension). The residual tangential stress at the edge was −17,000 psi (compression). The distribution of residual stress is shown in Fig. 42. Inelastic deformation must have occurred in the hot central region which contracted more than the edge during cooling.

Table 41—Effect of Welding Conditions on Residual Stresses in Edge-Welded Plates. Jamieson[63]

Specimen	Average Shortening of Gage Lengths in Central Strips, Inch per Inch	Maximum Tensile Stress psi	Location	Maximum Compressive Stress psi	Location	Average Compressive Stress in Central Portion of Specimen, psi
1*b*	0.000192	17,200	Just inside the weld	7,600	3 inches from weld	4900
2*b*	0.000299	18,100	At weld	7,500	" " " "	5600
1*c*	0.000275	26,800	" "	7,900	" " " "	6600
2*c*	0.000268	36,600	" "	10,300	" " " "	7100

Bollenrath[2] used the Mathar Method to determine the residual stresses in an annealed mild steel plate 0.59 inch thick, 24 inches square, torch heated along a center line 3 to 4 inches wide until the surface melted. A maximum residual tensile stress of 71,000 psi was observed in the heated zone. The tensile strength of the mild steel was about 53,000 psi.

The residual stresses in a soft steel plate 1.18 inches thick and 18 inches square that had been torch heated in the middle to about 400° C. and aircooled were measured by Siebel and Pfender.[5] Their wedge extensometer was used and the specimen was sawed into strips $1^1/_2$ inches wide after cooling. Residual radial and tangential tensile stresses up to 23,000 psi, Fig. 43, were observed on the heated surface, 14,000 psi on the other surface. A maximum residual tangential compressive stress of 14,000 psi was observed on the heated surface. The yield point of the steel was 36,000 to 37,000 psi.

Using the Sachs method, Kallen and Nienhaus[82] determined the radial, axial and tangential residual stresses in a flame hardened, medium manganese steel axle 4 inches diameter. The core hardness was 20 Rockwell C; the surface was 50 C. The hardness was below 30 Rockwell C at a depth of $^3/_8$ inch. At the center of the axle the radial and tangential residual stresses were +57,000 psi; the axial residual stress was +128,000 psi. The tangential and axial residual stresses at the surface were compressive; axial = −185,000 psi, tangential = −170,000 psi. At $^1/_2$ inch below the surface the axial residual stress was zero; the tangential residual stress was zero at $^{13}/_{16}$ inch from the surface.

According to Sarazin[16] (see section on Beads Deposited on Surface), a bead of deposited metal on the surface accounts for only 30% of the deformation, while the heat accounts for the remainder. The results quoted above also show that residual stresses in mild steel plates heated locally to relatively low temperatures may attain high values depending on the degree of localization of the heated zone. An evaluation of the "degree of localization" for typical cases (size of plate, metal, temperatures, etc.) remains to be made.

Artificial Cooling

It is well known that shrinkage distortion in welding can be reduced by artificially cooling the vicinity of the weld. Kautny[72] in 1922 recommended liquid air for the purpose, an anonymous writer[73] in 1933 recommended a cold water spray on both sides of a corner weld in oxyacetylene welding sheet steel to reduce distortion, and the water spray sometimes used in spot welding doubtless serves a similar purpose.

Although it may reduce distortion, artificial cooling may or may not have a bad effect on residual stresses, apart from the question of its bad effect in promoting the formation of brittle martensite in many steels. Notvest[74] states that water cooling accentuates residual stress, Müller[67] believes that temperature gradients should be as low as possible, but the experimental work of Melcher[66] and Bollenrath[2] did not reveal artificial cooling as an important contributor to residual stresses in an edge-welded plate or in arc-welded butt joints. As a result of his experiments, Gehring[21] concluded that uniform cooling of plate and weld creates less residual stress than cooling of plate faster than weld or vice versa. Bühler and Lohmann[7] showed that water cooling of welds in steel disks generally increased the residual stress, but the effect may or may not be important depending on the size of the heat-affected zone in comparison with the size of the disk.

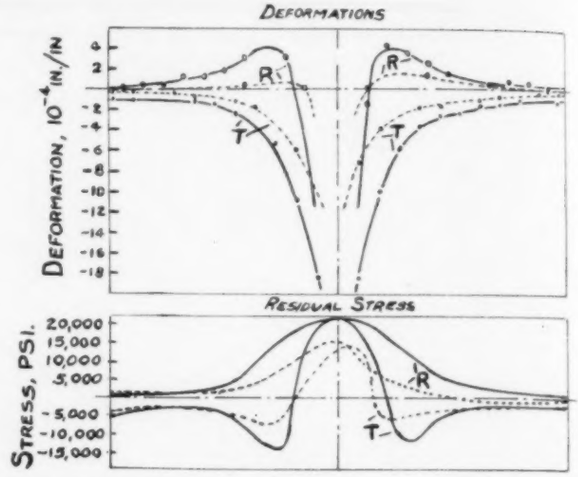


Fig. 43—Deformations and Residual Stresses in a Mild Steel Plate Which Has Been Heated in the Center to About 400° C. by Means of a Torch, and Cooled in Air

———— upper face of plate (torch side)
– – – – – – lower face of plate
R = radial
T = tangential
Siebel and Pfender[5]

Preheating

Preheating is said[75] to prevent a good deal of distortion in welding heavy castings. There is practically no quantitative information on the effect of preheating in preventing distortion but it is certain, as pointed out in the Reviews on Cast Iron and Cast Steel, that preheating may reduce the tendency to cracking, provided the preheating temperature does not coincide with a brittle range of the material to be welded. Since stress together with ductility and stress distribution is one of the important contributing factors to cracking, it may be taken as axiomatic that preheating reduces residual stresses if the type of cooling from preheat temperature is favorable.

Roš and Eichinger,[76] and Sonderegger[26] found by qualitative tests that a preheating temperature of 300° C. removes a large part of the residual stress in oxyacetylene and arc welds in steel. The former attributes the effect to an "easing" of the austenite transformation. Boyd and Cape[77] found that a preheating temperature of 200° F. (95° C.) was sufficient in some cases to eliminate cracking which occurred if the preheating temperature fell below 150° F. According to Benson,[78] 200° F. is far too low to avoid residual stresses. Bergfeld[79] describes preheating practice for a welded plate girder, and an anonymous writer[80] in 1913 used preheating to reduce residual stresses in burn-on welds of locomotive wheel spokes and cast-iron cylinders.

Stress Relief by Annealing

The removal of residual stresses by heat treatment is common practice for many products, for example, glass, brass tubing, castings of all sorts. Quantitative data on time-temperature relationships for removal of residual stress are available for many of these products, but a general quantitative theory has not yet been developed. Stress relief by annealing is probably a result of inelastic action, such as creep, viscous or plastic flow, as Müller[81] and many others have pointed out. However, time-temperature relationships applying to one type of material under a given type of residual stress do not apply to the same material under a different type of stress, nor to a different material under the same type of internal stress. In the present review, therefore, only information related to welded joints or weld metal is discussed. Although the various investigations are in reasonably

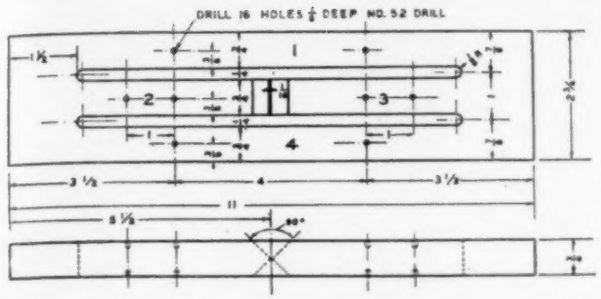


Fig. 44—Specimen Used by Jennings[83] for Measuring the Relief of Residual Stresses by Annealing

close agreement, their results apply only to the type of residual stress distribution prevailing in their welded specimens. It is not to be expected that their results apply to other types of residual stress distribution, nor to other types of welds, materials of different composition or heat treatment, or different welding procedures. Little information is available on the locations of creep or other inelastic action in a welded structure during stress annealing.

An extensive research on stress annealing arc welds in hot-rolled, low-carbon steel has been made by Jennings,[83] Fig. 44. Seventeen specimens were welded (procedure not stated) using $^5/_{32}$-inch bare electrodes, 140 amp. The gage holes were specially reamed $^1/_8$ inch deep, No. 52 drill. Berry gages were used having an accuracy of 700 psi in 4 inches. The root spacing of the 90° X weld was $^1/_{16}$ inch. Specimens were heated to the stress annealing temperature in 45 to 60 minutes and were cooled in the furnace. Gage readings were made before and after welding, after stress annealing, and after making a saw-cut through the middle of the weld. The gage readings probably indicated the relief of some combination of welding and reaction stresses perpendicular to the weld. The stresses were calculated by means of Young's Modulus, assuming that the stress in the welded portion and side frames was uniformly distributed over the cross section. The work of Mies[3] and Lance Martin[18] has shown (for specimens of somewhat different dimensions) that this assumption is scarcely justified. The results are shown in Fig. 45. Jennings stated that yielding of base metal is just as effective as yielding of weld metal in relieving residual stresses, but his specimens apparently did not reveal where yielding occurred.

A determination of the percentage removal of residual stress in unwelded and welded plain carbon (less than 0.2% C) and nickel steel ($3^1/_2$ Ni with about 0.30% C) was made by Barnes.[84] The specimens were 20 inches long, 1 inch wide $^1/_4$ inch thick with two transverse welds or one longitudinal weld, unmachined. The welds were made with low-carbon welding rod (no details). The unwelded specimens were bent cold around a $2^1/_2$-inch mandrel; welded specimens were bent cold around a $3^1/_2$-inch mandrel. The effect of the cold work was eliminated presumably by preheating all cold bent specimens to 300° F. (150° C.) before initial measurement. The stress was applied to the specimens by means of a bolt across the free ends. The bolt (type of steel not stated) was drawn up nearly to the elastic limit of the bend as determined by measurements (0.008 inch accuracy) across the bend. The specimens developed no permanent set after $2^1/_2$ hr. at this loading. All

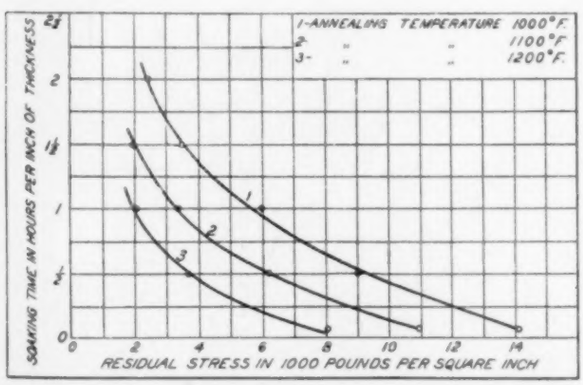

1-ANNEALING TEMPERATURE 1000°F.
2- " " 1100°F.
3- " " 1200°F.
SOAKING TIME IN HOURS PER INCH OF THICKNESS
RESIDUAL STRESS IN 1000 POUNDS PER SQUARE INCH

Fig. 45—Chart Showing Results Obtained in Annealing Welded Test Plates for Different Periods of Time and at Different Temperatures. Jennings[83]

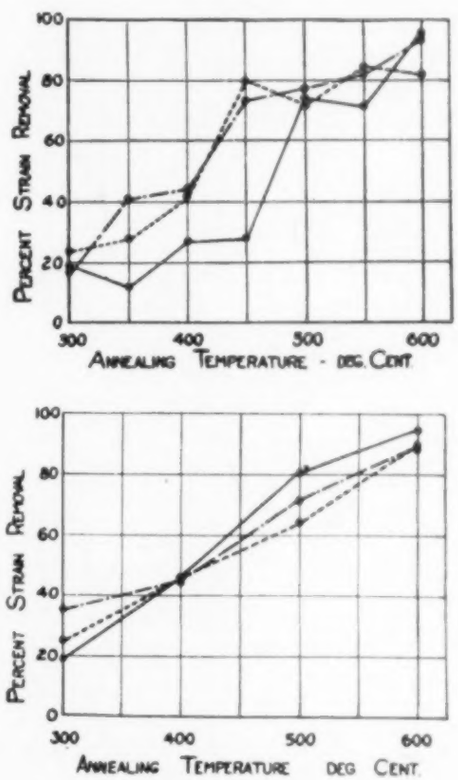


Fig. 46—Strain Relief

———— not welded
———·—— longitudinal weld
– – – – – transverse weld
Lower Diagram: Nickel steel plate, ¼ inch thick (3½ Ni, about 0.30 C), and low-carbon steel welding rod.
Upper Diagram: Plain Carbon Steel plate, ¼ inch thick (less than 0.20 C) and low-carbon steel welding rod.
Watertown Arsenal (Barnes[84])

plain carbon and the unwelded nickel steel specimens were held 20 minutes at test temperature; the welded nickel steel samples were held 1 hr. The specimens were cooled in the electric furnace (3 hr.). The permanent set was measured after the bolts were released. The permanent set divided by the elastic distortion before heating defined the "relative strain removal" due to stress annealing.

The results are shown in Fig. 46. The plot of percentage strain removal *vs.* temperature for the unwelded specimens nearly coincided in trend with the plot of percentage decrease in yield point *vs.* temperature for both steels. The strain removal curve was somewhat low below 500° C. probably because the time factor is important at the lower temperatures. The results for the welded specimens were similar to the unwelded. These

Table 43—Residual Stresses, psi, in Stress Annealed Disks with a Bead of Covered Electrode Weld Metal Deposited on Both Sides. Bühler and Lohmann[59]

Stress annealing temperature, degrees C.	20	100	200	300	400	500	600
Tangential and radial stress at center; mild steel St 37	35,700	35,700	27,000	20,000	15,700	7,100	1400
Tangential and radial stress at center; low-alloy structural steel St 52	45,500	44,100	40,000	34,200	25,700	20,000	4300
Tangential stress at edge; mild steel St 37	25,700	25,700	24,200	20,000	11,400	5,700	2800
Tangential stress at edge; low-alloy structural steel St 52	20,000	18,500	17,100	15,700	11,400	10,000	1400

experiments showed that the welded specimens underwent more inelastic deformation at low temperatures (300° C.) than the unwelded, but that the reverse was true at higher temperatures (500 to 600° C.). It is not stated to what extent the experiments represent relief of residual welding stresses, although such relief would probably have been reflected in the results.

As described in the section on Welded Boilers, Ebel and Reinhard[52] found that local torch annealing was of no value for relieving residual stresses in oxyacetylene welded drums, whereas thorough annealing gave complete relief. In this connection it may be mentioned that Jurczyk[85] found normalizing yielded complete release of welding stress; improper normalizing may, of course, set up residual stresses of its own. Bierett and Grüning[4] (see section on Oxyacetylene Welds with Restraint) also found that the effect of local reheating of oxyacetylene welds with a torch did not materially relieve the residual stress, although the difference between the residual stresses on root and face sides of the welds was considerably reduced. Jacobus[86] and Kinzel[87] regard local stress annealing as generally unsatisfactory, but Davy[88] points out that local stress annealing for early American welded pressure vessels 3 to 4 inches thick for oil field service was satisfactory. Von Roessler[8] observed a large reduction in residual stresses in gas welds by local stress annealing (see section on Oxyacetylene Welds with Restraint). He also found that residual stresses in flame-cut mild steel was completely removed by heating at 600° C. (no details). Theisinger[11] found that the maximum residual stress in a stress relieved, automatic arc butt weld in $^7/_{16}$-inch structural steel was 4000 psi. Details of stress relieving are not given nor are results for a similar weld not stress annealed.

The stress annealing of surface welded specimens has been studied by Bühler and Lohmann and by Jamieson.[63] Bühler and Lohmann used the Sachs method and procedure described in the section on Oxyacetylene Welds with Restraint. The disks were 9.7 inches diameter, 0.79 inch thick, with beads deposited on the surface or in grooves 0.20 inch deep on both sides. The grooves were 0.20 inch wide at the bottom, 0.50 inch wide at the top. The diameters of the circles in which welding was done were $3^1/_8$, $5^1/_2$ or 6 inches. Covered electrodes were used.

The results shown in Table 43 apply to a bead 0.32 inch wide deposited on a circle $3^1/_8$ inches diameter. No difference was observed between annealing times of 2, 6 and 10 hours. The results from grooved and bead-welded specimens with different diameters of circle were about the same. The tangential and radial stresses at the center were tensile and, of course, were identical. The tangential stress at the edge was compression. Stress relief by annealing was considered to occur by minute plastic deformations caused by the decrease in elastic limit with rise in temperature.

The specimens studied by Jamieson[63] were duplicates of those used in his second series of experiments described in the section on Beads Deposited on Edge of Plate. The stress annealing consisted of slow heating to 1150° F. (620° C.), holding 1 hr., and slow cooling. The mechanical properties of base metal after stress annealing were: tensile strength 50,900 psi, yield point 31,050 psi, elongation 31.1% in 8 inches, reduction of area 65%.

As shown in Table 44, the residual stress parallel to the welded edges in the stress annealed specimens was below 5000 psi, which is said to be the proportional limit of base metal at 1150° F. (620° C.).

Table 44—Effect of Welding Conditions on Residual Stresses in Stress Annealed, Edge-Welded Plates. Jamieson[63]

Specimen	Average Shortening of Gage Lengths in Central Strips, Inch per Inch	Maximum Tensile Stress psi	Location	Maximum Compressive Stress psi	Location
1*b*	0.000091	1800	Just inside of weld	4400	At weld
2*b*	0.000163	1500	Just inside of weld	3300	At weld
1*c*	0.000200	2900	At welds	3200	2 inches from welds
2*c*	0.000243	4500	At welds	1800	2 inches from welds

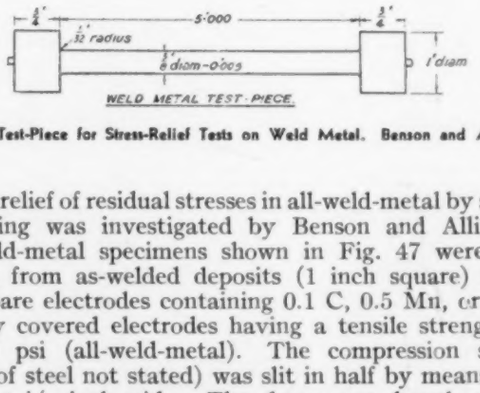


Fig. 47—Test-Piece for Stress-Relief Tests on Weld Metal. Benson and Allison[90]

The relief of residual stresses in all-weld-metal by stress annealing was investigated by Benson and Allison.[90] All-weld-metal specimens shown in Fig. 47 were machined from as-welded deposits (1 inch square) made with bare electrodes containing 0.1 C, 0.5 Mn, or good quality covered electrodes having a tensile strength of 67,000 psi (all-weld-metal). The compression sleeve (type of steel not stated) was slit in half by means of a saw cut $^1/_{16}$ inch wide. The sleeve was shrunk on the specimen from 250–300° C. Measurements of the entire length of each specimen were made before and after heat treatment, and after removal of the sleeve by machining.

The extent of stress relief at 550 and 600° C. is shown in Table 45. Measurements (no details) showed that relief occurred mainly in weld metal rather than in the sleeves, which had four times the cross section of weld metal. Although residual welding stresses may have played a part in Benson and Allison's specimens, they, like Barnes', showed the ability of weld metal to relieve

itself of externally applied stress of a relatively simple distribution, not of residual welding stress.

Table 45—Relief of Induced Stress in All-Weld-Metal by Stress Annealing. Benson and Allison[90]

Electrode	Treatment	Calculated Initial Tensile Stress, psi	Final Stress, psi	Percentage Stress Relief
Bare	5 to 6 hr. at 550° C.	27,000	1800	90
Covered	"	26,500	6700	75
Bare	5 to 6 hr. at 600° C.	10,000	less than 1100	over 90
Bare	"	29,000	"	"
Covered	"	23,400	"	"

Experiments similar to those made by Benson and Allison have been carried out by Stewart[160] on all weld metal (0.09 C, 0.010 S, 0.003 P, 0.55 Mn, 0.015 Si, 65,000 to 70,000 psi tensile strength) deposited by mild steel electrodes, $^3/_{16}$ inch diameter between two $^3/_4$-inch carbon steel plates. The specimens tested represented three conditions of deposition: namely, metal deposited at such a rate that the temperature did not exceed 250° F. (120° C.) for each pass; 320° F. (160° C.) for each pass; and 400° F. (205° C.) for each pass. The specimens were 0.50 inch diameter, 4 inch gage length (Huggenberger extensometer) and were strained to the yield point by tightening bolts at both ends, which projected through holes in a heavy steel frame.

The results are shown in Table 46. The specimens were heated at a slow rate in order to ensure uniform temperature in the heavy straining frame.

Although it was not stated whether the maximum temperature attained during welding refers to the entire plate or to the scarves, the results show that the higher the temperature at which the metal is deposited, the more effective is the stress annealing treatment, especially at an annealing temperature of 900° F. (480° C.). The stress given in Table 46 is in psi and is the stress remaining in the specimen at room temperature after heat treatment but before removal from the straining frame. The yield point of the weld metal is not stated. However, it is reasonably certain that the results provide some information on the extent to which the yield point is lowered by temperature, although the creep factor also enters. Stewart seems to have believed that the shrinkage stresses created by welding exerted some unexplained effect on the relief of stress.

The effect of residual stress of stress annealing a locally heated plate 0.59 inch thick, 8 inches square, of mild steel (yield point 50,000 psi, tensile strength 71,000 psi) was studied by Siebel and Pfender[5] by means of the subdivision method (strips $1^1/_2$ inches wide) and their wedge extensometer, 0.79-inch gage length. The plate was heated in the center of both faces to 400° C. by means of two torches simultaneously, which produces maximum radial and tangential residual stresses of about 30,000 psi. The annealing periods were $^1/_2$, 2 and 4 hrs. at 240, 360, or 480° C. The maximum tensile residual stress in the plates annealed at 480° C. was 14,000 psi, at 360° C., 17,100 psi and at 240° C., 31,400 psi.

According to Bullock,[91] the microstructure of welds in plain-carbon mild steel is altered (no details) by stress annealing at 700 to 1100° F. (370 to 590° C.). Charles[92] states that prolonged stress annealing may have a bad effect on the structure of a welded joint (no details).

Theisinger[11] also observed changes in the microstructure of arc welds in low-alloy steels (SAE 4150) after $^1/_2$ hour at 1150° F. (620° C.). The changes were those to be expected during tempering an air-hardening steel. The microstructure of arc welds in SAE 1035 was not changed by stress annealing. It would appear that, for welds in low-carbon, unalloyed and low-alloy steels, in the absence of martensite in the original weld, stress annealing at ordinary temperatures (600° C.) for the usual times may have a negligible effect on the microstructure, provided effects due to precipitation of copper or other constituents is excluded. If the stress annealing is prolonged (heavy masses) or at a relatively high temperature, some agglomeration of carbide may be expected in accordance with Bailey's equation.

The effect of stress annealing on the mechanical properties of welds has been dealt with by a large number of investigators, the results of only a few of whom are summarized in the present report (see Reviews on Fatigue, Impact and Cast Steel for additional information). Hodge [93] showed that stress annealing at temperatures up to 400° C. had no effect on the Rockwell hardness of all-weld-metal deposited by a bare (0.014 C, 0.13 Mn, 0.12 N_2) or a covered electrode (0.10 C, 0.43 Mn, 0.24 N_2). The drop in hardness was less than 10% even at 700° C.

Using the Vickers Brinell machine on 5 x 3 inch fillet-welded joists, Dr. L. Reeve (private communication, September 1937) found that a single layer fillet weld on mild steel (67,000 psi tensile strength) made with covered electrodes may pick up enough carbon from the plate to raise the hardness to 220 Brinell. Stress relief softens the weld to 180 Brinell. The heat-affected zone will have a partly sorbitic structure (200 Brinell) which softens to 170 Brinell on stress relief. The effect of stress annealing on hardness is not so marked in multi-run fillet or butt welds owing to the self annealing effect of subsequent runs.

Warner[94] found that stress annealing welds in low-alloy steels ($3^1/_2$% Ni, 0.30 C type) raises the impact value 50 to 100%. Stress annealing at 1150° F. (620° C., Jamieson[95]) or 1100° F. (590° C., Roark[37]) raised the tensile proportional limit to coincide with that of unwelded base metal (mild steel). Thum and coworkers[60] found that stress annealing (3 hrs. at 650° C.) of arc

Table 46—Release of Externally Applied Stress in All-Weld-Metal by Annealing. Stewart[160]

Annealed at		Temperature Which Metal Did Not Exceed During Each Pass: 250° F. (120° C.)	320° F. (160° C.)	400° F. (205° C.)
900° F. (480° C.)	1 hr.	24,000	19,000	15,000
"	4 hr.	22,000	18,000	14,500
1000° F. (540° C.)	1 hr.	13,000	not determined	not determined
"	4 hr.	10,000	not determined	not determined
1100° F. (595° C.)	1 hr.	9,000	not determined	not determined
"	4 hr.	not determined	not determined	7,000
1200° F. (650° C.)	1 hr.	not determined	5,000	not determined
"	4 hr.	not determined	not determined	4,000
1300° F. (705° C.)	1 hr.	not determined	not determined	3,000
"	4 hr.	2,000	not determined	not determined

welds in mild steel raised the pulsating tension fatigue limit by 2800 psi, which was also the effect of normalizing ($^1/_2$ hr. at 920° C., air cool).

Present practice in stress annealing welded work is summarized by Jennings.[96] He states that stress annealing is: (1) necessary for parts that must be machined without distortion; (2) desirable for parts in fatigue or impact or upon which large amounts of welding have been done.

The best temperature is 1100 to 1200° F. (590 to 650° C.). The best rate of heating is 140 to 250° F. per hr. (80 to 140° C.), the preferable rate being 200° F. per hour (110° C.). The furnace should not be above 300° F. (150° C.) when the welded material is placed in it, and the material should not be removed until the furnace has cooled below 300° F. (150° C.). Although Jennings[97] states that he knows of no one stress annealing at temperatures as low as 900° F. (480° C.), Hiemke[98] adopted this temperature for stress annealing a large steel turbine casting patched by arc welding. Hopkins[99] points out that stress annealing of welded pressure vessels is just as important for removing fabricating stresses as welding stresses. The A. S. M. E. Boiler Code Committee decided (Case No. 834) that welded unfired pressure vessels of 18–8 (0.07% C max., stabilized by either columbium or titanium) must be stress annealed at 1550° F. (845° C.), 1 hr. per inch of thickness followed by slow cooling in a quiet atmosphere.

Stress Relief by Overstrain

The relief of residual stress by overstrain occurs by the opposite process to that by which residual stresses may be developed in a stress-free material by local overstrain, such as cold work. Stress relief by overstrain employs a uniform stress to relieve local residual stresses. The regions most highly stressed internally in the same direction as the applied external stress may undergo local inelastic action if the yield point of the material under the particular distribution of residual stresses is exceeded. On release of the external stress uniform elastic recovery occurs over the entire cross section, the residual stresses being less than before overstrain to the extent of the inelastic action. Overstrain is thus a convenient term to express inelastic deformation at a stress above the elastic limit or yield point.

Opinion is divided on the practical value of overstrain as a means of relieving residual stress. Beckmann[100] states that residual stresses equivalent to the yield point are lowered by external load. Reeve[101] refers to welded pipe lines which were deliberately overloaded during test to reduce residual stresses. According to Mathar,[6] overstressing evens out the residual stresses, but impact overstressing may cause cracks. However, Dorey[102] regards mechanical straining as an undesirable means of relieving residual stresses in welded pressure vessels.

Experiments show that overstrain, apart from other possible effects, is effective in reducing residual stresses in welded joints. Bierett[4,103] investigated the reduction of residual stresses parallel to the weld caused by an external tensile stress of 28,500 psi applied ten times in a direction perpendicular to the joint. The specimens were butt-welded mild steel plates (minimum tensile strength 53,000 psi, length of weld 8 inches, length of plate perpendicular to weld, 20 inches, thickness 0.47 inch). Bare and covered electrodes were used having the mechanical properties shown in Table 47. The covered electrode was intended for high-tensile steel, not for mild steel. Consequently, high residual stresses were created.

Table 47—Mechanical Properties of Electrodes (All-Weld-Metal) Used by Bierett[4]

Electrode	Elastic Limit, psi	Tensile Strength, psi
Bare	28,500 to 43,000	60,000
Covered	78,000	86,000

The reduction of residual stress parallel to the weld caused by overstrain is shown in Fig. 48. The results apply to the center section of the welded plates (center line perpendicular to weld) and are given in terms of "reduced" stress, that is the stress calculated after cuts made parallel to the weld only.

The maximum residual tensile stress parallel to the weld and in the center of the bare electrode weld was 18,500 psi before overstrain and 8500 after ten applications of a tensile stress of 28,500 psi. Corresponding values for the covered electrode weld were 25,700 and 20,000 psi, respectively. The permanent elongation undergone by the bare electrode specimen was only 0.026%; the corresponding value for the covered electrode specimen was 0.023%. It is not stated in what region of the specimens the permanent deformations occurred.

The effect of overstrain on residual stresses in welded drums has been studied by Bollenrath,[51] who used the arc and oxyacetylene welded drums described in the

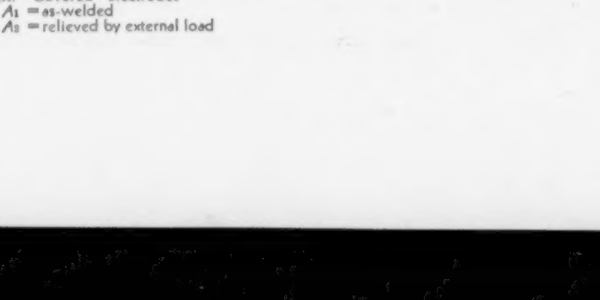


Fig. 48—Reduction of "Reduced" Residual Stress Parallel to a Butt Weld by the Application of External Load Corresponding to a Tensile Stress of 28,500 psi

Left: Covered electrodes
A_1 = as-welded
A_2 = relieved by external load

Right: Bare electrodes
B_1 = as-welded
B_2 = relieved by external load

Bierett[4]

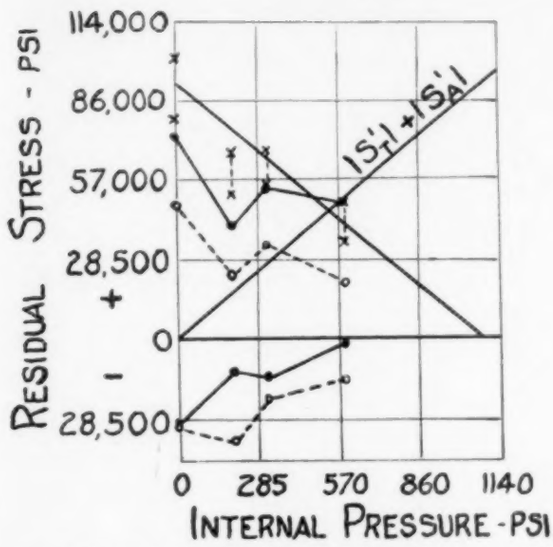


Fig. 49—Reduction of Residual Stresses in an Arc-Welded Boiler Drum by Internal Hydrostatic Pressure

○ = circumferential weld; average
⊙ = axial weld; average
× = $S_T + S_A$
S_T = circumferential (tangential) stress, as-welded
S_A = axial residual stress, as-welded
S'_T = circumferential stress created by internal pressure
S'_A = axial stress created by internal pressure. Bollenrath[61]

section on Welded Drums. Upon application of internal water pressure to the arc-welded drum, in steps of 2800 psi (calculated stress), residual stresses were decreased as shown in Fig. 49. Stress-strain curves recorded at several Mathar holes showed surprising irregularities indicating that neither the magnitude nor the direction of the residual stresses can be determined from such curves alone. On account of residual stress, inelastic deformation occurred at a calculated stress due to internal pressure of only 16,700 psi in the axial direction, and 33,500 psi in the circumferential direction. If the drums had been free from residual stress the elastic limit should have been reached at axial and circumferential stresses of 33,000 to 36,000 psi. Fig. 49 shows by extrapolation that residual stresses would disappear at a calculated stress (combined axial plus circumferential) of 88,500 psi. At a calculated combined stress of 34,000 psi (internal water pressure of 1080 psi) the algebraic sum of axial and tangential residual stresses was reduced by a factor of 0.55.

The oxyacetylene welded drum was tested at 1080 psi internal water pressure after which the circumferential seams had practically no axial residual stress and very little tangential stress. The tangential residual stress in the longitudinal seams was also decreased to nearly zero, but the axial residual stress was not appreciably changed.

Siebel and Pfender[5] also studied the effect of internal pressure on the residual stress in the vicinity of a circular, oxyacetylene welded patch, 6 inches diameter in a boiler drum 28 inches diameter, 0.55 inch wall thickness. The patch bulged inward after welding. The drum was made of mild steel, yield point 36,000 to 37,000 psi. Deformations around the patch parallel and perpendicular to the axis of the drum were measured after 1, 100, 848 and 10,000 cycles of internal water pressure creating a calculated stress cycle of 2000 to 20,000 psi. The patch recovered nearly 1% of the bulge after the first loading. Subsequent cycles led to an additional recovery of only 0.25%.

Bollenrath[2] and Buchholz[104] stressed butt welds in 0.32-inch mild steel plate along the direction of the weld. The welds were 70 inches long; the specimens were 8 inches wide. The edges of the specimens for a width of 2 inches on either side of the weld were water cooled during welding to accentuate residual stresses. An external tensile stress of 31,000 psi was applied to all specimens. Residual stresses before and after overstrain were measured by the Mather Method. The results are shown in Table 48. Small external loads caused permanent deformation in all specimens; the deformation was localized mainly at the junction of weld with plate.

Table 48—Reduction of Residual Stress Parallel to Weld by Application of External Load Parallel to Weld. Bollenrath[2]

Type of Welding	Oxy-acetylene	Arc Bare Electrodes	Arc Covered Electrodes
% decrease of residual stress	78	62.5	33.2
Residual stress, psi, remaining after removal of load	7100	13,300	32,700

The influence of external stress parallel to the weld on residual stress has also been studied by Åkeson[12] who used the subdivision method described in the section on Arc Welds Without Restraint. Sixteen specimens of mild steel (53,000 psi tensile strength) 0.39 inch thick 4 inches wide, 22 inches long containing an X weld, 22 inches long in the center were prepared, Fig. 50. The welds were made with 0.16-inch covered electrodes in four layers on each side. Gage lengths were established on the welded plate which was then loaded and then partly cut along six lines parallel to the weld. The residual stresses due to welding and overstrain were thus determined.

The results are shown in Table 49. In general the residual stresses commenced to decrease at a stress close

Table 49—Reduction of Residual Welding Stresses by Overstrain. Akeson[12]

External Stress, psi	Maximum Residual Tensile Stress in Weld, psi	Maximum Residual Compressive Stress at Edge, psi
0	28,500	17,100
4,550	31,500	30,000
10,200	21,300	30,000
11,900	22,800	31,500
17,000	21,300	25,700
18,800	28,500	25,700
20,500	28,500	17,100
25,600	28,500	20,000
31,700	21,300	17,100
34,100	14,200	15,700
37,000	11,400	17,100
39,800	20,000	7,100
42,600	11,400	7,100
45,500	7,100	7,100
47,600	7,100	4,300
49,800	11,400	7,100

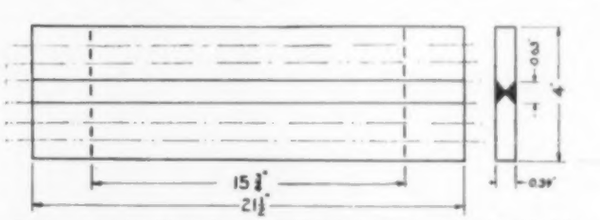


Fig. 50—Specimen Used by Akeson[12] for Determining the Release of Residual Stress by External Tensile Load. Four Layers Were Deposited on Each Side of the X, Using 0.16-Inch Covered Electrodes

—•—•—•— cuts made from each end nearly to middle of specimen
Gage length = 15¾ inches

to the probable yield point of the mild steel. Åkeson did not state the elongation of the specimens at the higher stresses. He also recorded load-deflection curves for a welded beam (no details). Permanent deflection of 0.012 inch occurred after the first loading to 330,000 lb. Subsequent loadings of the same amount caused no additional permanent deflection.

The effect of cycles of pulsating tensile stress on residual stresses in a soft steel plate 8 inches square, 0.59 inch thick heated at the center of both sides by torches to 400° C., and subsequently air-cooled, has been studied by Siebel and Pfender.[5] After the first cycle of external stress from 5700 to 28,500 psi there was a permanent elongation of less than 0.1%. The permanent deformation was not appreciably increased by an additional 10,000 cycles. The maximum residual tensile stress was reduced from 35,500 psi before loading to 22,800 psi after 10,000 cycles. According to Bernhard,[105] several welded railway bridges in Germany have been stress relieved by means of an oscillator.

Load tests on arc-welded mild steel plate girders (85 ft. span) by Reinhold and Heller[106] using Huggenberger extensometers on the tension flanges as well as deflectometers, showed that as the load was increased, the ratio of calculated to measured stress decreased from 3.36 to 1.45. The maximum residual stress was 15,700 psi, tensile in web, compressive in flange. Obviously inelastic action reduced the residual stresses. A tension test of an inclined butt weld (no details) showed that the yield point of base metal was considerably exceeded before fracture occurred.

An instance of release of residual stress by overstrain is supplied by a test made in Paris and quoted by Kommerell,[107] on the specimen shown in Fig. 51. The welded center bar contained tensile residual and reaction stress; the side frames were in compression. Upon application of a static tensile load such that permanent deformation occurred in the side bars, the residual compressive stress in the side frames disappeared.

The main conclusions to be drawn from the experiments are:

1. That external stress applied to welded joints may cause permanent deformation in regions containing residual stress. The permanent deformation corresponds to relief of local residual stress;
2. That the external stress required to cause local permanent deformation in the region corresponds to that which will bring the region past the "yield point" or elastic limit under the system of residual stresses that is present;
3. That the local permanent deformations to which relief is due occur near or in the weld where the highest residual stresses are found.

No general recommendations can be made for the magnitude of the external stress to be applied except that the sum of the external stress and the maximum residual stress should exceed the elastic limit of the material, the elastic limit depending, of course, to some extent on the system of residual stresses. Whether Buchholz's[108] assertion that welds made under constraint have lower residual stresses than welds made without constraint is true appears to depend on the magnitude of the reaction stress provided by the constraint. In some cases, as Bierett[4] showed, Buchholz's statement is not true, but it is conceivable that reaction stresses may act in some cases as external loads to relieve residual stresses in welded joints.

It should be noted, as Bierett points out (private communication, October 1937), that all the results on relief of shrinkage stress by overstrain have applied to mild steel.

The efficiency of peening as a form of mechanical stress relief has been mentioned in earlier sections of the review.

The Time Effect in Stress Relief at Room Temperatures

The gradual release of residual stresses in welded steel at room temperature under the influence of time or of time and chance shocks and vibrations has never been amply demonstrated. It is certain that dimensional changes at room temperature occur over long periods of time in quench-hardened steels. It is also known that stress relief by annealing occurs in welded steel at temperatures below that required to change the tensile strength and hardness at room temperature, (see section on Stress Relief by Annealing). However, no one at present can confirm either Hunter,[109] who confidently states that residual stresses in welded joints disappear to a considerable extent in the course of time, or Benson[78] who says they do not.

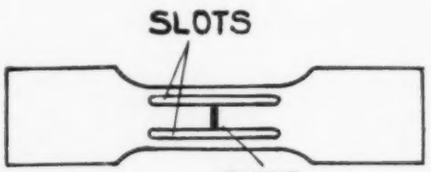


Fig. 51—Butt-Welded Specimen Described by Kommerell[107]

The only experiments not of a negative sort appear to be those of Lawson,[110] Lance Martin[18] and Graf.[61] Lawson found that all gage lengths on his fillet-welded plates (see section on Fillet Welds) had contracted in the course of a year, the contractions corresponding to the release of incredibly high residual stresses. On the other hand, Graf,[61] who permitted a mild steel plate on whose surfaces two short beads of weld metal had been deposited (see section on Beads Deposited on Surface) to remain at room temperature for 11 days, observed a very slight increase in distortion (no details). Lance Martin was unable by direct measurement to detect any sign of relief of residual stress in welds that had been allowed to remain at room temperature for long periods without any external loading.

Apparently, relief of residual stress at room temperature may occur by creep, or by overstress. The manifestation of creep applying to relief of residual stress is probably largely relaxation or creep at decreasing stress. Creep speeds for relaxation are usually larger than for creep at constant or increasing stress. Several metals, notably tin, lead and copper, have appreciable rates of creep at room temperature. Creep has not been detected in iron or steel under constant stress at room temperature. The tests have not envisaged possible effects of vibration, occasional impact, or other disturbing factors.

Welds Made Under Stress

Axial extensometer measurements were made by Jurczyk[111] on a mild steel bar (40 x 3.2 x 0.67 inches) under constant tensile stress (1400, 8500 and 14,200 psi) during deposition of four transverse metal arc beads at the same section on the surface. Table 50 shows that the constant tensile stress caused enough permanent deformation in the heated zones to counteract the normal

contraction of the surface welded plates, except at the lowest stress. The time of welding and other details were not given, but the time between starting successive beads was 3 minutes. By increasing the time between beads to 10 minutes at a constant stress of 8500 psi, and spacing the welds 12 inches apart, the elongation immediately after depositing the last bead was only 0.051 inch, and after cooling to room temperature 0.009 inch. Evidently, the heat effect was less intense in the latter test than in the test listed in Table 49 so that the constant tensile stress had less opportunity to cause permanent deformation of the heated material.

Table 50—Axial Distortions of Surface Welded Plates Under Constant Stress. Jurczyk[111]

Constant Tensile Stress, psi	Distortion, Inch (+ = elongation; − = contraction) Immediately After Depositing Fourth Bead	After Cooling
1,400	+0.020	−0.012
8,500	+0.087	+0.037
14,200	+0.236	+0.165

Diatlov[112] recorded the load in mild steel plates held under a constant tensile strain during arc welding. The residual stress in the X welded plates after cooling was very small.

Experiments similar to Jurczyk's were made by Colam and Watson,[113] who used covered electrodes (no details). Four series of tests were made.

1. Wrought iron bars 24 x 4 x $^3/_8$ inch were held in a jig to prevent buckling and loaded in compression to stresses of 0 to 32,000 psi. Two or six arc weld beads were deposited along the length of one surface of each bar, the load being maintained constant. After the specimen had cooled to room temperature it was removed from the jig and the curvature and reduction in length were measured, Table 51.

Table 51—Residual Bending Stress in Bars Welded in a Jig Under Constant Compressive Stress. Colam and Watson[113]

Number of Weld Runs	Constant Compressive Stress, psi	Reduction in Length, Inch	Equivalent Bending Stress Caused by Buckling, psi
6	32,000	$^3/_{16}$ to $^1/_4$	10,200
6	16,000	0.186	7,900
2	16,000	0.078	5,700
2	9,600	0.041 to 0.046	1400 to 2400
2	6,400	0.040	420
2	3,200	0.043	560
2	0	0.032	280

The higher the initial compressive stress and the greater the number of weld beads, the higher is the residual bending stress.

2. In the second series the wrought iron bars, which were 12 instead of 24 inches long, were not held in a jig. At external stresses of 5100 or 10,200 psi, two transverse beads caused failure by buckling. A single longitudinal bead did not cause buckling, the contraction varying from 0.0205 to 0.028 inch independent of stress. The contraction per unit length was about the same as for specimens held in a jig.

3. Mild steel bars 24 x 3 x $^1/_2$ inch were stressed in tension while two beads of weld metal 16 inches long were deposited on one surface along the center line, Table 52. No explanation was offered for the fact that the

Table 52—Residual Bending Stress in Bars Welded Under a Constant Tensile Stress. Colam and Watson[113]

Constant Tensile Stress, psi	Average Change of Length, Inch (+ = Elongation: − = Contraction)	Residual Bending Stress, psi, Due to Curvature
6,700	−0.025	6100
13,400	+0.013	2800
26,900	+0.225	500

residual bending stress decreased as the applied tensile stress increased. The changes in length are in qualitative agreement with Jurczyk,[111] but the latter found much higher unit changes. Undoubtedly, the explanation is that Jurczyk studied transverse beads which heated the entire loaded section. Colam and Watson, on the other hand, studied beads which brought only a small proportion of the bar to a temperature such that the applied stress exceeded the proportional limit.

4. I beams 7 x 4 inches with an effective span of 48 inches were tested in three point loading with and without a plate 24 x 5 x $^3/_8$ inch welded to the compression flange after the stress reached 20,000 psi. The load was maintained constant during welding. There was a permanent deflection of $^3/_{32}$ inch without increase of load during welding. The proportional limit of the unwelded beam was 62,000 psi; that of the welded beam was 64,000 psi.

The conclusion reached by Colam and Watson is that residual stresses are created during welding a stressed member. Part of the stressed member is heated so that the remainder of the section is overstressed.

Effect of Residual Stress on Tensile Properties

The effect of residual stresses on the elastic limit or yield point of welded material has been investigated by Jamieson.[64] He determined stress-strain curves for edge-welded mild steel plates (see section on Beads Deposited on Edge of Plate) 5 feet long, $4^3/_4$ x $^5/_8$ inch cross section. The plates were stress relieved at 1150° F. before welding. A U-shaped groove on both edges (0.08 sq. in. groove cross section) was filled by means of $^5/_{32}$-inch covered electrodes (110-120 amp. 23 volts arc, electrode negative) suitable for vertical and overhead welding. The welds were made on a 45° slope, single pass lower to upper end. Copper chills, $^1/_8$ x $1^1/_2$ inches, on each side of the groove prevented burning of the edges. The ratio of cross sections of welds to base metal was 0.0575. Skrinkage stress measurements by the subdivision method on similar plates but only 30 inches long revealed residual stresses parallel to the welded edges of −17,000 psi (compression) at the center and + 18,000 to + 23,200 psi (tension) at the edges. Stress-strain measurements on the 5-ft. plate during tensile testing were made on both sides with the aid of four Martens extensometers accurate to 0.0001 inch in 8 inches and located 1 inch away from each welded edge, as well as with a steel scale accurate to 0.01 inch in 30 inches along the center line of the plate.

The stress-strain curves of the welded specimens diverged from the straight line obtained for unwelded at a stress of 3000 psi. Since the maximum residual tensile stress was about 25,000 psi and the yield point was 29,700 psi it had been expected that divergence would occur at 4700 psi. The observed deformation of the welded specimens averaged 4% more than unwelded at 16,000 psi and 15% more than unwelded at 24,000 psi. At the yield point the break in the stress-strain curve was ab-

rupt for unwelded but gradual for welded plates. The effect of partly unloading and reloading the welded specimen was the same as was to be expected from unwelded specimens. The yield point of the welded plates was 1000 to 2500 psi higher than unwelded. The average ultimate strength was 52,900 psi for unwelded and 48,100 psi for welded plates, the difference being ascribed to unequal distortion along the edges of the welded plates which led to premature local failure. It has been noted in the section on Stress Relief by Annealing that a welded specimen annealed at 1150° F. (620° C.) was shown by Jamieson to have practically the same yield point, tensile strength and elongation as unwelded.

In addition to Jamieson's investigation, there are a large number of instances in the literature in which the yield point of welded specimens has been reported, such as Roark's[37] quoted in the section on Plug Welds. In the absence of information regarding the maximum residual stress in the specimens it is hardly worth while to review their data. Jamieson's results seem to make it amply clear that the tensile elastic limit of a welded specimen is lowered to an extent approximately proportional to the maximum residual tensile stress. The yield point and tensile strength are scarcely affected by stress.

Effect of Residual Stress on Buckling

Residual stresses caused by welding having been shown to have an effect on the elastic behavior of steel, it is conceivable that residual stresses may have a considerable effect on the strength of welded structures, such as columns, in which elastic stability is the determining factor. However, experiments have shown that, for the columns tested, residual stresses do not have pronounced effects on buckling.

The columns studied by Bierett and Grüning[40] consisted of two lengths of as-rolled mild steel *H* section ($8^1/_2$ x $8^1/_2$ inches, web thickness 0.39-inch flange thickness, 0.63 inch, $43^1/_2$ lb./ft.) each 5 feet long. The lengths were butt welded together by 90° X welds in web and flanges, 0.08-inch root spacing, the welding being done with covered electrodes by a structural engineering firm. All welding was done in a horizontal position. The outer side of the flanges was welded from one side to the other; the inner side of the flanges was begun from the web. The welding of the first layer of the web was commenced from the middle of the web; other layers were continuous from one flange to the other. Copper strips were placed at the edges of the flange to obtain a neat joint. Welds were made in four layers, two at the root, then two on top. The sequence of welding is shown in Table 53.

Table 53—Sequence of Butt Welding *H* Columns. Bierett and Grüning[40]

	Welding Sequence			
Column	First Operation	Second Operation	Third Operation	Fourth Operation
B	Two root layers on both flanges	Two root layers on web	Two top layers on both flanges	Two top layers on web
C	Two root layers on web	Two root layers on both flanges	Two top layers on web	Two top layers on both flanges

Residual stresses in the butt-welded *H* columns were determined by subdivision of the welded zone. The results are shown in Table 54.

Table 54—Residual Stresses Perpendicular to the Weld at the Outer Edges of the Flanges of Butt-Welded *H* Columns. Bierett and Grüning[40]

Column	Number of Columns Studied	Residual Stresses, psi Average	Range
B	1	−34,000	−24,000 to −43,000
C	2	− 2,800	0 to −5,700

The compression zone at the edge of the flange of column *B* extended only 0.6 to 0.8 inch back of the edge, which is considerably less than for plain butt-welded plates. Residual compressive stress due to rolling and mill factors up to 8500 to 11,500 psi was found in the flanges of the unwelded, as-rolled columns.

The columns had milled ends and were tested in a hydraulic machine with knife edge bearing plates. The tensile properties of specimens (0.6 x 0.8 x $8^1/_2$ inches long) machined from the flanges of the unwelded columns averaged (5 specimens):

Proportional limit, 26,000 to 33,000 psi; Lower yield point, 34,000 to 36,500 psi; Upper yield point, 34,000 to 38,000 psi; Elongation, 29.8 to 33.2% in 7 inches; Reduction of Area, 64 to 67%; Modulus of Elasticity, 29,500,000 psi; Buckling Limit, 38,500 psi.

The specimens for buckling were 0.57 x $1^1/_4$ inches, 3.2 inches long, machined from the flange of an unwelded column. The welded and unwelded *H* columns were tested with axial and eccentric load (eccentricity = 1 inch away from axis of web). Load-deflection curves were plotted for the joint and near the ends of the columns.

The buckling loads are recorded in Table 55. Evidently, the butt-welded columns had nearly the same buckling strength as the unwelded under axial or eccentric load, despite high residual compressive strengths (near the buckling stress) in the outer portions of the flanges of one type of welded column. The maximum difference between welded and unwelded was only about 4%. The load deflection curves showed that the welded columns underwent greater deflections before buckling than the unwelded.

Table 55—Buckling Loads for Butt-Welded and Unwelded *H* Columns. Bierett and Grüning[40]

Column	Axial Load, Lb.	Eccentric Load, Lb.	Permissible Buckling Stresses, psi Axial	Eccentric
Unwelded	430,000	234,000	34,100	18,500
B Welded	411,000	233,000	32,700	18,500
C Welded	409,000 (average of 3 columns)	231,000		

The effect on buckling strength of stitch welding two 11 x $^5/_8$ inch cover plates to the flanges of a 10 inch, 35 lb. I beam, $5^1/_2$ ft. long was studied by Slater and Fuller,[114] who provide no details of welding procedure. It was estimated that the welds had tensile residual stress up to the yield point and that regions remote from the weld were in compression up to 15,000 psi. In testing the columns, strain lines appeared first in the regions which showed compression after welding. The calculated buckling loads agreed closely with the experimental.

Wilson and Brown[46] determined the buckling strength of riveted columns, 20 ft. long, with and without welded reinforcement, Fig 52. Residual distortion due to welding corresponded to residual stresses of 10,000 to 15,000 psi, but in some cases equaled the yield point on gage

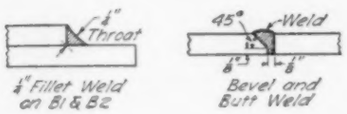
Schedule of Welding Procedure

Columns B1 and B2

1-Clamp plates P3 & P4 to flanges of Z-bars, in exact position, and tack by one inch of weld at intervals of 18 inches.
2-Butt weld P4, near side, at top of column.
3-Fillet weld P4, near side, about 18 inches, laying bead continuously from top down, first on one edge, then on the other edge.
4-Butt weld P4, far side, at top of column.
5-Fillet weld P4, far side, same as (3).
6-Continue fillet weld for P4, alternating near side and far side, as above, until entire length of plate is welded.
7-Butt weld between P4 and P3.
8-Fillet weld P3, same procedure as with P4.
9-Butt weld P3 at bottom of column.

Columns C1 and C2

1-Clamp plates P5 and P6 in exact position on column and tack by one inch of weld at intervals of 3 feet.
2-Fillet weld P5, near side, at bottom of column first one edge then the other, laying bead continuously from bottom up.
3-Make intermittent, staggered welds and shoe-string welds, near side, for about 3 feet, laying bead continuously from bottom up.
4-Make fillet welds, far side, same as (3).
5-Repeat (3) & (4) alternating near side and far side to end of plates.
6-Butt weld between P5 and P6.
7-Fillet weld P6 same as P5.
8-Butt weld P5 at bottom of column.



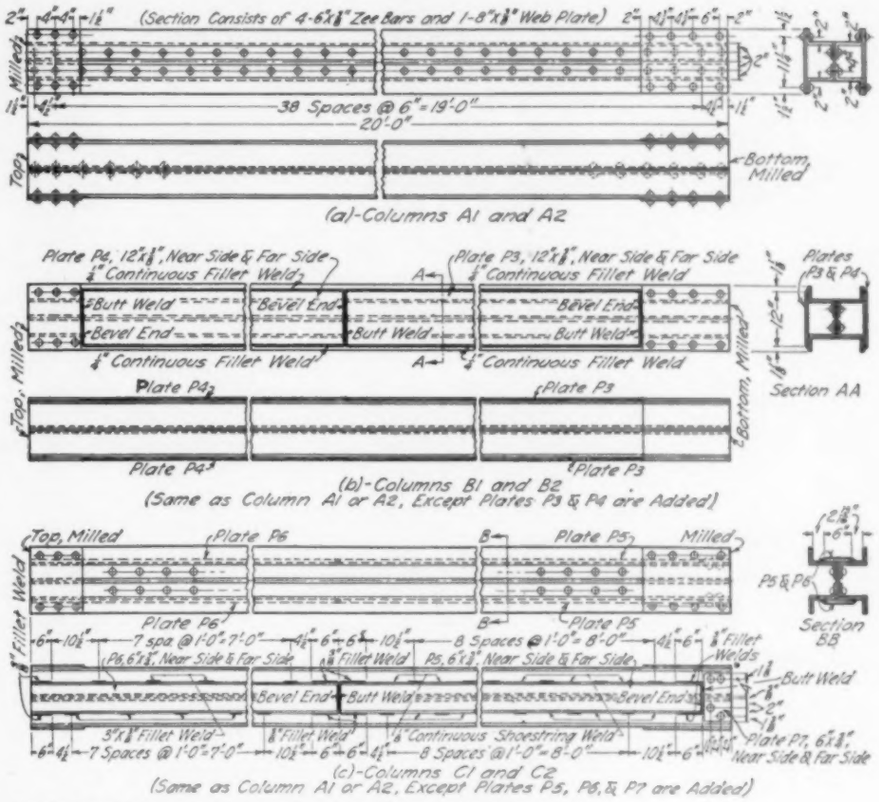

(Section Consists of 4-6"x⅜" Zee Bars and 1-8"x⅜" Web Plate)
38 Spaces @ 6" = 19'-0"
20'-0"
Top, Milled
Bottom, Milled
(a)-Columns A1 and A2
Plate P4, 12"x⅜", Near Side & Far Side
Plate P3, 12"x⅜", Near Side & Far Side
⅜" Continuous Fillet Weld
Butt Weld
Bevel End
Plates P3 & P4
Section AA
Plate P4
Plate P3
(b)-Columns B1 and B2
(Same as Column A1 or A2, Except Plates P3 & P4 are Added)
Top, Milled
Plate P6
Plate P5
Milled
Fillet Weld
Section BB
Butt Weld
Continuous Shoestring Weld
Plate P7, 6"x⅜", Near Side & Far Side
(c)-Columns C1 and C2
(Same as Column A1 or A2, Except Plates P5, P6, & P7 are Added)

Fig. 52—Details of Laboratory Specimens Tested by Wilson and Brown[46]

lengths remote from welding. The welding distortion did not affect the straightness of the columns, which were tested with axial load, spherical bearing blocks, fixed ends. Column deflection was measured at the mid-point; column strain was measured at five sections 4 ft. apart, twenty extensometers being used at each section. The yield point of the Z bars was 35,700 to 39,600 psi in tension, 34,100 to 38,300 psi in compression. The tensile yield point of the reinforcing plates for *B* columns was 34,400 psi, for *C* columns 36,900 psi. Table 56 shows that if the reinforced column is straight its unit load carrying capacity will be as great as that of unreinforced columns. The calculated yield point in Table 55 is the sum of the yield point strengths of the various parts of the column determined from tests of specimens. The *C* columns were slightly better than the *B* columns perhaps because the outstanding legs of the *Z* bars which initiated failure by wrinkling, were affected by the welding heat.

A striking example of the absence of effect of residual welding stresses on the buckling of an arc-welded, mild steel frame, Fig. 53, has been reported by Schaper.[115] Calculation indicated that, if the residual welding stresses were effective in reducing strength, the corner should have failed at a flange stress of 17,100 psi. Actually, the corner withstood a flange stress of 35,500 psi without noticeable distortion. At this stress the capacity of the testing machine had been exceeded and the test had to be discontinued.

Effect of Residual Stress on Fatigue

A lengthy review of the effects of residual stresses on the fatigue strength of welded joints was made in the review of literature on Fatigue Strength of Welded Joints. Investigations have since been made on the subject by Bierett and Grüning,[38] and by Thum and coworkers.[50]

A summary of the results obtained by Bierett and Grüning[38] on welded I beams and T beams with butt welds at the mid-section was published in the May 1937 issue of THE WELDING JOURNAL, pages 10–11. The residual stresses in the beams are described in the section on Welded Beams. The beams were welded without constraint and were tested in pulsating bending, the external tensile stress in the outer fibers being counteracted in many of the beams by residual compressive stress in these fibers. Fracture often originated at the tension

Table 56—Buckling Strength of Riveted Columns with and Without Welded Reinforcing Plates. Wilson and Brown[47]

Column	Area of Cross Section, Sq. In.	L/R	Maximum Load, Lb.	Maximum Stress, psi	Calculated Yield Point, psi	Ratio of Maximum Load to Calculated Yield Point
A_1; not reinforced	21.36	66	714,000	33,400	750,500	0.95
A_2; " "	21.36	66	726,000	34,000	740,800	0.98
B_1; Continuous fillet welds	30.36	64	1,000,000	33,000	1,044,900	0.96
B_2; " " "	30.36	64	986,000	32,500	1,037,700	0.95
C_1; interrupted fillet welds	30.36	75	1,080,000	35,600	1,069,000	1.01
C_2; " " "	30.36	75	1,060,000	34,900	1,075,000	0.99

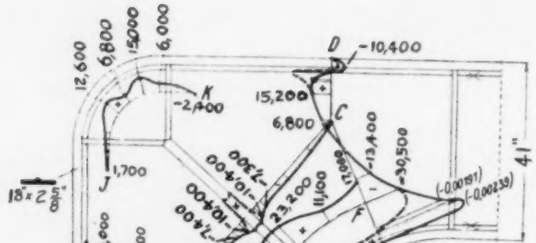


Fig. 53—Residual Stresses (psi) in a Frame for a Tunnel Determined by a Non-Destructive Method. The Dotted Lines Indicate the Probable Course of the Stresses in the Heat-Affected Zones Near the Weld Which Could Not Be Reached by the Instruments Employed. Figures in Parentheses Are Measured Compressions in Millimeters, Part of Which Was Obviously Permanent, Not Elastic. The High Tangential Residual Compressive Stress Near Point F Is Considered Particularly Significant for the Strength of the Frame. Web Is 1 1/4 Inches Thick. Schaper[115] (German Materials Testing Bureau; Bierett and Gruning[40])

edge of the web. The endurance limit of the beams (0 to maximum tension in outer fibers) was 27,100 to 30,000 psi. The static tensile strength of the steel was about 53,000 psi. Bierett and Grüning concluded that residual tensile stresses (or compressive stresses) due to welding beams are of secondary importance to fatigue strength.

The same conclusion was reached by Thum, Kaufmann and Schönrock[60] as a result of pulsating tension fatigue tests on specimens of the kind described in the section on Beads Deposited on Surface. Specimens of this kind (mild steel) with a short or long bead of weld metal on one or both sides deposited longitudinally by bare, light-covered, heavy-covered, or austenitic electrodes showed a 42 to 50% decrease in pulsating tension fatigue strength compared with unwelded specimens. A bead deposited across the width of the specimen on one side decreased the fatigue strength only 25%. The investigators attribute the difference to residual stresses.

A side fillet-welded joint with the ends of the fillets protected by the compressive residual stresses (no details) due to additional neighboring beads, Fig. 54. had a slightly improved pulsating tension fatigue strength, but porosity and the shape factor still remain the most damaging factors in fatigue.

Thum and Erker[116] investigated the effect of local torch heating on the pulsating tension fatigue strength of mild steel specimens (0.79 x 2.35 inches, 10 inches parallel portion) with mill scale. A specimen without torch heating had a pulsating tension fatigue limit (2 x 10^6 cycles criterion) of 38,800 psi (lower stress 2100 psi) whereas the corresponding value for a specimen heated to a red heat on one side by a torch from one end of the parallel portion to the other along the center line followed by air cooling, was 38,200 psi. Fracture originated at the edge of the unheated specimens, but in the center of the heated area of the torch-heated specimens, despite the fact that the heat-affected zone had a finer grain size and as a consequence, probably, a higher fatigue strength than base metal. Therefore, residual stresses (no details) must have had an effect on fatigue strength. Apparently the distortion due to heat was not sufficient to cause eccentric loading.

Service fatigue failures are occasionally attributed to residual stress. For example, von Schwarz[117] reports a cast steel crankshaft of a water pump, the web of which (2 3/4 inches thick) had been surface welded to fill in shrinks. The web failed by fatigue, the fatigue crack originating in the weld deposit. The unwelded cast steel had very low notch impact value and was fairly hard (183 Brinell). Nevertheless von Schwarz blames the failure on residual stress, which he did not attempt to measure, and states that, had the shaft been stress relieved after welding, the failure was much less likely to occur.

Cracks

Strictly speaking, the subject of cracks in a weld and its vicinity is more closely connected with the metallurgy and physical properties of the welded joint than with residual stresses. Nevertheless, residual stresses, by a combination of their magnitude and distribution at any point, are unquestionably necessary to start a crack. There appears to be no acceptable theory at present to predict the conditions (stress, temperature microstructure, etc.) under which a crack (except, perhaps, a fatigue crack) is initiated. As a consequence, cracks are now dealt with on an empirical basis: Some combinations of circumstances are favorable to cracks, others are not. The following paragraphs summarize representative statements about these circumstances. The reviewers note a deplorable absence of systematic investigations on cracking in welded joints made by all processes in all alloys.

Probably the most comprehensive statement of the present position is made by Bierett,[39] with whom, of course, everyone will not agree. In the first place, Bierett, who tacitly confines his discussion to steel, lays it down that cracks may occur due to residual stresses at two stages during the production of a welded joint: (1) immediately after solidification; (2) in the blue brittle range (220–300° C.). Swinden[118] is of precisely the same opinion. Fig. 55 taken from the work of Hall[119] shows that cast steels have indeed little ductility at temperatures just below the solidus. According to Piwowarsky and coworkers,[120] who determined the tensile properties of cast steel (acid and basic Bessemer and electric steel, 0.2 to 0.4% C) at 650 to 1450° C. using much slower testing speeds than Hall, there is a maximum reduction of area at 1250° C. above which the ductility suddenly decreases to nil.

The second stage is by no means so general, many steels such as 18–8 having no blue brittle range, although they may develop brittleness in other dangerous temperature ranges, in which the ratio of strength, under normal and shear stresses is unfavorable to ductility, or in which embrittling microstructural changes occur. Bierett considers it questionable whether cracks occur after cooling without external load. In any case, if such cracks appear, they are probably initiated at elevated temperatures, or caused by welding a neighboring seam.

Bierett divides cracks in welded steel into four types: (*a*) cracks due to unsuitable quality of weld metal (mainly in fillet welds), (*b*) cracks due to hardening in

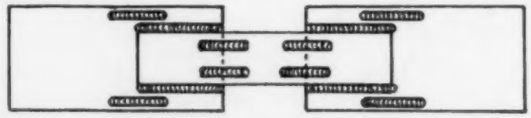


Fig. 54—Side Fillet Welded Fatigue Specimen with Beads of Weld Metal Deposited on Surface to Create Compressive Residual Stresses at the Ends of the Side Fillet Welds. Thum, Kaufmann, and Schönrock[60]

higher carbon and alloy steels, (*c*) cracks next to the weld, particularly in thin chromium-molybdenum steel, (*d*) cracks of mechanical (design) origin.

Unfortunately, Bierett gives no illustrations of the cracks he describes. W. D. Chapman[121] agreed with Bierett. The only service cracks in welds due to shrinkage stresses known to Chapman were caused by bad welding and brittle weld metal and occurred before the weld had cooled down. Bruff[122] also found that service cracks in bridges reinforced by welding occurred before or after welding was completed or shortly after application of live load. Rewelded fractures never cracked. The cracks in wrought iron and mild steel cited by Bruff were found mainly in base metal.

Agreeing with Bruff, Dr. Lewis Reeve (private communication, September 1937) finds that cracking after welding is completed is almost invariably due to purely metallurgical factors, such as embrittlement or hardening. The decisive factors for crack formation in order of importance appear to be: (*a*) hardening of base metal adjacent to weld, (*b*) degree of restraint, (*c*) C and Mn content of electrode (other alloys not yet studied), (*d*) welding technique. It is possible to avoid cracks in restrained joints, even when there is considerable hardening of base metal, by employing suitable electrodes, details of which will soon be made public.

Bierett regards the type of filler rod (covered or bare, etc.) as an important factor in reducing cracks in so far as ductility at high temperatures is improved. The reduction of stress concentrations he considers even more important. Notch effects created by careless deposition of the first few layers of a weld may give rise to cracks which are propagated through the entire weld. In fillet welds, small fillets are always prone to crack. Since residual tensile stresses at the face of a fillet weld are increased by using a concave weld, the flush or convex weld is preferable from the standpoint of avoiding cracks. Preheating and reheating are useful means of avoiding cracks. Peening is of doubtful value. Cold peening requires a ductile weld metal. Hot peening of base metal next to the weld is useful in avoiding cracks in welds made under rigid constraint. Hot peening also closes pores which may serve as nuclei for cracks.

The reviewers had hoped to show as a result of a statistical survey of reported cracks, that there was some relationship between the location of a crack and the alloy or welding process involved. However, no simple relationship appears to exist, cracks occurring in base metal and in weld, transverse or parallel to the weld without apparent relation to the system of residual stresses that may have prevailed, or to other variables. Sayers,[123] in 1918, observed that cracks occurred mainly in the heat-affected zone parallel and perpendicular to the direction of the plate, the direction of rolling having a little effect. Owens,[124] as a result of experiment, found that cracks occurred in the regions of maximum deformation due to welding. Possible connection between flakes in alloy steels and cracks in welding these steels has not yet been found. The suggestion[126] that flakes in oxyacetylene welds arise from oxide films is a misunderstanding.

Cracking relieves residual elastic stress, as Reinhard[9] showed. In oxyacetylene welding a slit 12 inches long in a mild steel plate 60 x 48 inches, Reinhard found that after making the first cut of subdivision the plate cracked. Subsequent subdivision caused no change in gage lengths. Relief of residual stress by cracking was, therefore, complete. Reeve[101] believes that the ability of bare electrode welds to resist shrinkage cracks is explained by microscopic cracks (no details) which relieve residual stresses. Reeve also reports (private communication, September 1937) that hardening of low-alloy steel plate near the weld is sometimes more marked with thick plate than with thin, and that under some conditions the hardening causes cracks adjacent to the weld. The more rapid conduction of heat in thick plates than in thin may account for the hardening.

Cracks in welded air-hardening or high-strength aircraft tubing and sheet have been dealt with by George[127] and Bollenrath and Cornelius,[55] among others. George found that cracks generally occur in base metal adjacent to the weld, and are caused by a combination of fabricating and welding stresses. If the tube is heated momentarily to a red heat by torch before welding, the fabricating stresses are removed and the tendency to cracking is reduced.

A factor that may be important for residual stresses and cracks in welded joints is grain size. The early belief, as expressed by Berck,[128] was that grain coarsening has an effect on residual stress as a result of movement among the grains. However, it is fairly certain that since grain growth usually implies a very low, if not absence of, elastic strength, any effect due to grain coarsening would not be reflected in residual stress, which is elastic. Residual stress due to grain growth, apart from brittleness and aging, has not been observed in unwelded material to the reviewers' knowledge. According to Houdremont and Schrader,[129] the tendency of steels to crack during welding increases with the sensitivity of the steel to overheating. Aluminum-deoxidized steels are relatively insensitive to welding cracks, although the composition may be otherwise unsatisfactory, as Zeyen[130] showed.

Bollenrath and Cornelius[55] found that cracks in welded chromium-molybdenum and carbon-manganese aircraft steels are intercrystalline and have the same appearance as cracks developed in high temperature tensile tests at 800 to 1000° C. It should be recalled in this connection that cracks in martensitic steels occur in an intercrystalline manner even at room temperature and that intercrystalline cracks have been known to be initiated by reagents applied to the surface of a stressed metal. For example, molten bronze (possibly flux) on steel, or even acid on water-quenched steel such as ball bearings may open cracks that are not previously present.

It is well known that tensile stress, whether external or internal, favors intercrystalline penetration and cracking of steel by bronze. Riede[131] found that cracks occur in dip-brazed steel tubes (composition not stated) only if the bronze is above 1100° C., and only then if the steel

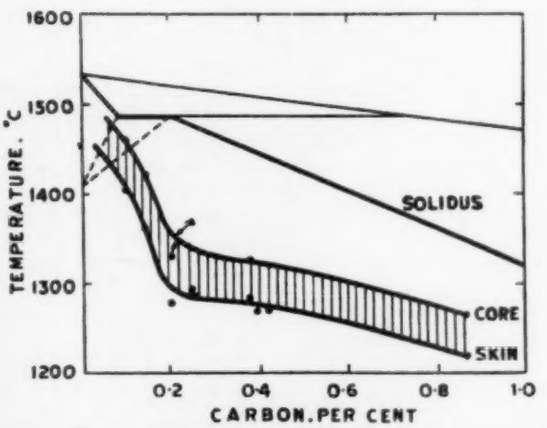


Fig. 55—Temperature of Cast Steel Tensile Specimens Giving 5% Elongation in 4 Inches. Above the Shaded Area the Elongation Is Less Than 5%; Below It the Elongation Is Greater. Rate of Strain = 0.21 Inch per Second

Steels contained about 0.3 Si, 0.6 Mn and not over 0.025S, 0.025 P. Heats were made in High-Frequency Induction Furnace. Hall[119]

is heavily cold worked and has been tacked before brazing. Fracture occurs 1 to 1 1/2 inches away from the weld where the temperature of recrystallization 700 to 800° C. has been attained. That the steel must be tacked to develop cracks shows that tacking causes recrystallization to coarse grains due to residual stress, the brazing brass flowing around the large crystals. Genders[132] observed intercrystalline penetration of brazing brass into mild steel containing 0.15% C at 850 to 900° C. if the steel was cold worked, or at 1000° C. if the steel was annealed. The subject of intercrystalline penetration of one metal by another at relatively low temperature is broad and cannot be pursued further in this review. Intercrystalline penetration, of course, is a form of cracking in that the grains of the base metal are forced apart.

Bollenrath and Cornelius[55] found that cracks may occur even below 250° C. in aircraft steels. According to Werner,[133] micro-cracks in welded aircraft steel occur at 700–800° C. (*A*3) in the region of very low tensile properties. If the *A*3 transformation is retarded in some way to 400–600° C. cracking may not occur because the steel is stronger. He also believes that unrestrained aircraft sheet may be welded without cracks even if the chemical composition is unfavorable and provided that the weld is not excessively long. Cracking occurs only if the sheets are restrained during welding. Bollenrath and Cornelius[134] found that right-hand and left-hand welding of Cr-Mo tubing gave about the same cracking results in these severe tests. Right-hand welding is generally preferred in practice, where, of course, the difficulty may be easily overcome by changes in welding technique, as tests at the Bureau of Standards have shown. Sensitivity to cracking increases with decrease in plate thickness and with excessive overheating during welding, as well as with increase in sulphur content at a constant carbon content in the aircraft steels. According to Swinden,[118] it is believed in some quarters that there is greater difficulty in avoiding cracks in welded plain carbon steel when the manganese content is between 0.3 and 0.6% than with higher or lower manganese content, but no definite experimental work has been published in confirmation.

Without wishing to create the impression that residual stresses are mysteriously dangerous or, alternatively, of no importance whatever as a cause of service failures, the reviewers refrain from listing instances reported in the literature of failures of welded parts considered to have been caused by residual stress. Although there are exceptional cases, such as those reported by Schuster[29] and Ehrt and Kühnelt,[59] there is usually sufficient ambiguity in the circumstances of failure to preclude the possibility of assigning the cause of failure to a single factor, particularly in the absence of experimental determination of the magnitude of the residual stresses involved in the case.

Crack-Sensitivity Tests

Methods for testing aircraft sheet steel (Cr-Mo and C-Mn, up to 2.25 Mn up to 0.10 inch thick) for tendency to cracking during welding have been described by Zeyen,[135] Bollenrath and Cornelius[134] and Müller.[136] According to Zeyen,[135] the tests are:

1. Fokker bead test. Deposit a bead on the surface of a square sheet, starting the bead at the middle of an edge and proceeding to the center.
2. Focke-Wulf bead test. Same as (1) but starting at a corner, proceeding to the center, then welding to the middle of an adjacent edge. Repeat at each corner.
3. Cross-shaped weld test. Two rectangular sheets placed one on top of the other and fillet welded. The sheets are then bent 90° at the welds.
4. T-Weld test. Bent sheets are welded at right angles to the surface of a flat sheet. (T-weld specimens generally show greater crack sensitivity than butt welds.)
5. Rigid assembly tests. Two rigidly gripped sheets are butt welded together. (Used as a specification specimen at present by several German aircraft works.)
6. Small sheet laid flat on surface of a larger sheet and fillet welded. Cracks appear on the under side of the large sheet.

In all these tests the crack sensitivity is the percentage of the total length of the weld occupied by cracks, as outlined by acid or temper colors. The methods are said to be unsuitable for inclusion in specifications because test results are sensitive to small variations in welding procedure. On the other hand, the tests are said to be becoming more popular as a direct indication of crack sensitivity, for which measurements of Vickers hardness, fatigue, impact value and tensile or bend ductility have been shown to be poor substitutes. The crack tests have been found to distinguish between steels refined by different processes. Overheating followed by pouring at the correct temperature decreases the crack sensitivity of a steel of otherwise unfavorable composition.

Some of the tests, such as No. 5, represent severe conditions, so that crack sensitivity percentages determined therefrom under standard welding conditions, appear to be used for grading steels as to crack tendencies. In some of the less severe tests, the occurrence of a crack may be cause for totally disqualifying a steel. The requirement of total absence of cracks is being applied to the more severe tests, as well.

Test No. 5, as described by Müller,[136] consists of gripping two strips (usually 0.04 inch thick, 2 inches wide) between two circular dies 2 3/4 inches apart whose temperature rises to 100° C. maximum during welding. Cracks occur mainly in the face of the weld. The crack sensitivity (ratio of cracked, oxidized cross section to total cross section) is a maximum for specimens 1 3/4 to 2 3/8 inches wide. The test is known as the Focke-Wulf test and is regarded as more severe than the test of Bollenrath or Gatzek. The former test grips the plates by means of tongue and groove dies so that the clamped length is accurately adjustable between 2 3/8 and 3 1/2 inches. In the Gatzek test the dies have plane surfaces and the clamped length is therefore indefinite. The clamped length should be adjustable in order to subject sheets of different thicknesses to texts of identical severity. The highest shrinkage stresses are considered to be developed in welds whose length is 40 times plate thickness, but in the Bollenrath test crack sensitivity appears to increase continuously as the gripped length is decreased. Since the Gatzek test does not rely on formed dies to crimp the sheet for clamping effect, it is applicable to heavier plate than the other two rigid assembly tests. According to Werner,[133] thorough investigations (no details) at the German Materials Testing Department have shown that the reaction stresses in the rigid assembly tests only become important at temperatures far below the temperatures at which cracking occurs.

For thick plates (3/8 in. or so) there appear to be no tests expressly for determining crack sensitivity. A Navy test for cast steel in which a central groove is filled with weld metal and cut up for examination undoubtedly gives some information about crack sensitivity. Suggested tests include the welding of patches in discarded boilers, the welding of a slit in a long plate, and the butt welding of plates rigidly gripped by artificial means. For example, Polson[137] believes that the groove or slot test in 1/4-inch plate is expensive and might well be replaced by filling in defects in steel castings. A specimen for

testing the crack sensitivity of electrodes used by the German Railways is shown in Fig. 56.

Zeyen (private communication, March 1937) suggests a crack sensitivity test for welds in thick plate consisting of the deposition of a bead of weld metal on the surface of a bend test specimen. A bend test is then performed on the specimen in the direction of, or perpendicular to, the bead. A steel with high hardening capacity will break more quickly in bending than a steel with low hardening capacity. Tests of this sort have been reported by Bierett.[138] Roller bend specimens 2 to $2^3/_4$ inches deep (low-alloy structural steel) with a bead of weld metal deposited on upper and lower surfaces cracked

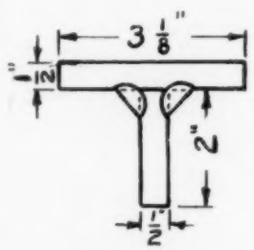


Fig. 56—Specimen Used by German Railways to Test the Crack Sensitivity of Electrodes Bierett[30]

at 12 to 18°, whereas unwelded specimens did not crack at 180°. The thicker the beaded specimen the smaller was the angle of bend, because the mass or quench effect was heightened. Willey[131] has also emphasized the importance of mass effect in cracked welds. As in any discussion of cracks in welding, the important factors in crack sensitivity are metallurgical; residual stress is a necessary contributing factor in the absence of external load.

Welds Other Than Mild Steels

There have been few quantitative investigations of residual stresses in welds in metals other than mild steel. Results for low-alloy structural steel have been reported by Bühler and Lohmann,[7] Gehring[21] and Gerold[41] (see sections on Butt Welds and Welded Beams). Consistent with its higher elastic strength at low and elevated temperatures, the low-alloy structural steel has higher residual stresses than mild steel for a given type of joint and welding procedure. Results for Cr-Mo aircraft steels are given in the section on Tubes, and for rail steel in the section on Fillet Welds. Residual stress in a mild steel drum welded with austenitic electrodes have been determined by Kautz[53] (section on Welded Boilers). According to Thum, Kaufmann and Schönrock,[60] austenitic electrodes may be expected to produce about the same residual stresses as the usual bare and covered electrodes in mild steel.

Practically nothing of a quantitative nature is available on residual stresses in non-ferrous welds, aside from general directions, such as Mittendorfer's,[140] on the correct root spacing to be adopted for thick beveled aluminum plates. According to Smith,[141] electric brazed deposits are sufficiently ductile to absorb much of the contraction and expansion stress.

Merica and Karr[142] (1916) demonstrated that residual stresses are a factor in the corrosion cracking of burn-on welds in manganese bronze castings in service. A portion of the bar, Fig. 57 near A, from $^1/_4$ to 2 inches long was sawed out and replaced by burning-on, the bar B being kept cool meanwhile. Gage lengths (8 inches)

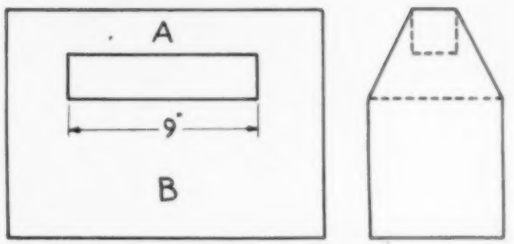


Fig. 57—Specimen Used by Merica and Karr[142] for Measuring Residual Stresses in Burnt-On Manganese Bronze Castings

were located within the 9-inch welded length and were measured before and after cutting out the burnt-on section. For welding, the casting (58.5 Cu, 39.1 Zn, 1 Sn, 1.4 Fe, Pb trace, Mn none) was placed on its side and embedded in green sand. The ends were preheated and a little molten bronze was poured on the ends as a preliminary. No flux was used. Bar B was sometimes cooled by passing water through a 1-inch hole drilled longitudinally. The microstructure was fine-grained and sound. As shown in Table 57, the residual stress decreases as the size of the burn-on increases. Cooling bar B with water (2-inch gap replaced by burning-on) increased the residual stress to 8500 psi. Calculation based on the known solid shrinkage of bronze showed that a tensile stress of 36,000 psi should have been developed for every inch burnt-on. Obviously inelastic yielding occurred during cooling of the actual specimen.

Table 57—Residual Tensile Stresses in Burnt-On Bar in Bronze Casting. Merica and Karr[142]

Length at A removed and replaced by burning-on, inches	$^1/_4$	$^1/_2$	2
Residual tensile stress in bar A perpendicular to burn-on, psi	9600	8500 to 9200	6400

Theoretical Aspects

Introduction

Considered from a rather general standpoint a theory of shrinkage stresses in even the simplest welded joints should predict the elastic and inelastic behavior (stresses and strains, both permanent and recoverable) of all parts of the joint during welding and cooling. The data for a general theory should consist of stress-strain-time relationships and thermal and elastic coefficients for the material at all temperatures up to the melting point and for many conditions of heat treatment. In the almost complete absence of many of these ideal data, it is not surprising that most of the theoretical studies of shrinkage stresses in welding have been based on the assumption that the welded joint is homogeneous and on the neglect of any inelastic deformation. While the theory based on completely elastic deformation has yielded valuable results, theories based on inelastic as well as elastic deformation are fundamentally better. The following sections summarize studies of these two theories, the order of approach being from the simple to the more complex.

The Elastic Theory

The basic physical idea of the Elastic theory, which assumes that the welded joint remains elastic at all temperatures, is contained in the experiment of heating an originally stress-free bar which is prevented from expanding axially by compressive forces at the ends. If

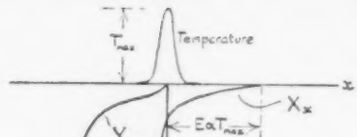


Fig. 58—Stress Distribution in Hottest Section of Locally Heated Strip

X_x = stress in direction of length of strip due to local rise in temperature of $T_{max.}$ across the entire width of the strip.
Y_y = corresponding stress in direction of width of strip.
Goodier[147]

the temperature of the bar is uniformly raised T degrees, its axial expansion per unit length, if free, is αT, α being the thermal coefficient of linear expansion, which is assumed independent of temperature and stress. The stress corresponding to the completely restrained expansion is $E\alpha T$. Upon cooling to its original temperature the bar assumes its original length, provided the elastic limit of the material has not been exceeded ($E\alpha T$ is equivalent to about 350 psi per degree Centigrade for steel; for a temperature rise of about 150° C. the corresponding stress is about 50,000 psi, depending on steel and temperature). The stress also returns to zero on cooling.

Similar considerations apply for the tensile stress produced in cooling an originally stress-free bar which is prevented from contracting axially by tensile forces at the ends. If the bar is free to change in length with temperature, no thermal stress is created. If, on the other hand, a constrained bar is subjected to a system of initial stresses of any kind, the thermal stresses are simply superposed. Non-uniform heating may act like constraint to produce thermal stresses. If thermal stresses are created by non-uniform heating they are quantitatively expressed in the Elastic theory as some multiple of $E\alpha T$, and dissappear when the body has resumed its initial temperature distribution.

Obvious application of the Elastic theory to simple restrained welded bars has been made by Owens,[124] Mies,[143] Nies,[144] Rosenthal[145] and Malisius,[146] among others. Mathematical difficulties surround the application of the theory to more complicated welded joints. A standard work outlining the general method of solution of thermal stress problems in three dimensions is Timoshenko's "Theory of Elasticity," pages 203 to 212. The solutions obtained for non-uniform distribution can be considered to apply to cases in which the part is considered stress-free under conditions of non-uniform temperature distribution, and to acquire thermal stresses upon subsequent cooling and equilization of temperature, if the sign of the temperature, and hence of the stresses, is reversed. According to J. N. Goodier (private communication, October 1937), if a stress-free plate at non-uniform temperature is considered, we bring it to uniform (zero) temperature by imposing temperatures equal and opposite to the original non-uniform distribution. This superposed distribution then brings with it the corresponding thermal stress; that is, the thermal stress of the original uniform temperature reversed in sign. The rate of temperature change at a point is always considered slow in comparison with the velocity of stress propagation in an elastic body. The solutions provided by the mathematical theory of thermal stress in elastic solids are not put forward primarily as theories of residual stress in welding. So far as welding is concerned, as J. N. Goodier (private communication, October 1937), has pointed out, all that the mathematical theory of elastic thermal stress can do is show that a rather small degree of heating can bring the steel to the yield point, and pave the way for a possible theory involving inelastic deformation.

Straight Butt-Welded Joint in Flat Plates of Infinite Length.—The thermal stresses in a straight butt-welded joint in plates of infinite length perpendicular to the weld have been calculated by Goodier[147] and Den Hartog.[148] Goodier obtained solutions for two cases. First, the infinitely long plate is at uniform temperature except for a band of uniform length across the width of the plate. The maximum temperature occurs near the center of the band, which is T degrees above the uniform temperature. The temperature of the band is independent of thickness, that is, both surfaces of the plate are at the same temperature at a given point (x, y). The temperature of the band is also independent of width; that is, both edges of the plate at a given section are at the same temperature. The form of the temperature distribution curve is immaterial.

The distribution of thermal stress across the width of the heated plate is shown in Fig. 58 at the section of maximum temperature. Den Hartog obtained identical results by a slightly different method. The maximum tensile stress is at the edge and parallel thereto, and is equal to $E\alpha T$. A compressive stress of the same magnitude perpendicular to the edges occurs in the middle of the section heated to T. The distribution is symmetrical about the center line of the plate. As a matter of interest it should be observed that the distribution of thermal stresses across the cross section happens to be opposite in sign to the distribution of residual stresses in butt-welded joints. There seems to be no theoretical explanation for this coincidence.

In the second case dealt with by Goodier[147] the two surfaces of the infinitely long plate are at different temperatures, the temperature distribution across the thickness being linear. Otherwise, conditions are the same as in the first case. The maximum thermal tensile stress turns out to be $\left(\frac{2}{3+u}\right) E\alpha T$ where u = Poisson's ratio. For the same maximum temperature, the maximum thermal tensile stress for steel in the second case is 0.61 of the stress set up by heating uniformly through the thickness. In applications of welding where thermal stresses alone are a consideration, it is therefore advisable to keep one face of the plate as cool as possible.

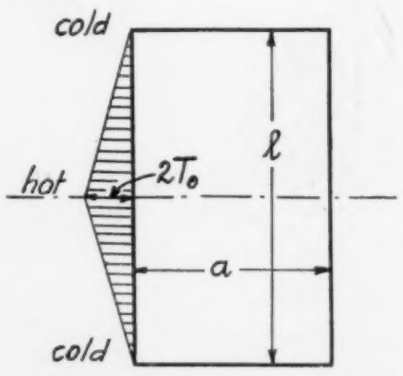


Fig. 59—Plate of Length l, Width a; Temperature Along the Center Line Is $2T_0$ Degrees Above Temperature of Ends. Den Hartog[148]

Straight Butt-Welded Joints in Plates of Finite Length.—Den Hartog[148] modified his treatment of infinitely long plates to obtain the approximate solution for thermal stresses in straight butt-welded joints in a finite plate, Figs. 59 and 60. In the first case the temperature varies linearly from the ends toward the middle, attaining maximum of $2T_0$ at the middle. The maximum tensile stress is shown in Fig. 61, curve B, for various ratios of length to width of plates. The ordinates of curve B must be increased about 4%, which arises from the approximate treatment necessitated by the condition of finite length. The maximum tensile thermal stress is 1.04 $E\alpha T_0$ (not $E\ \alpha\ 2T_0$) for extremely short plates. With increasing length the maximum thermal tensile stress parallel to the weld decreases.

In the second case, Fig. 60, considerable portions of the plates near the end remain at uniform temperature. If these portions are sufficiently large (no details) the maximum thermal tensile stress is shown by curve C, Fig. 61. The thermal stresses are considerably less than in the first example. Again, artificial cooling is beneficial in reducing thermal stresses in butt welding.

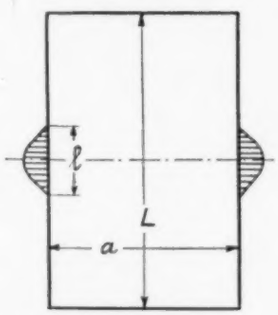


Fig. 60—Plate of Length L, Width a; Strip of Length l Across the Width of the Plate at the Center Line Is $2T_0$ Degrees Above the Temperature of the Ends. Large End Portions of the Plate Are Cold (Uniform Temperature). Den Hartog[148]

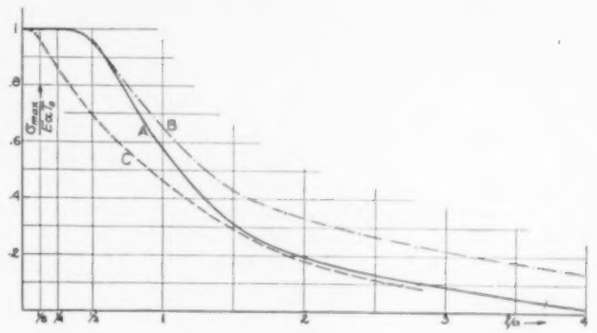


Fig. 61—Maximum Temperature Stress in a Strip of Infinite Length, Having a Heated Center Line. Den Hartog[148]

An entirely different method was used by Grüning[149] in calculating residual (not thermal) stresses in butt-welded plates of finite length. Grüning assumed that all stresses caused by heating for welding and during cooling could be neglected since they were dissipated by inelastic deformation of the hot regions until a maximum temperature of 600° C. was reached. No precise justification for the assumption is offered. Below 600° C. an average Young's Modulus of 22,500,000 psi is assumed for steel. Under these assumptions and with the aid of the general equations of elasticity for equilibrium and displacement in plane stress, it is shown that the Airy stress function for thermal stress has the same form as the

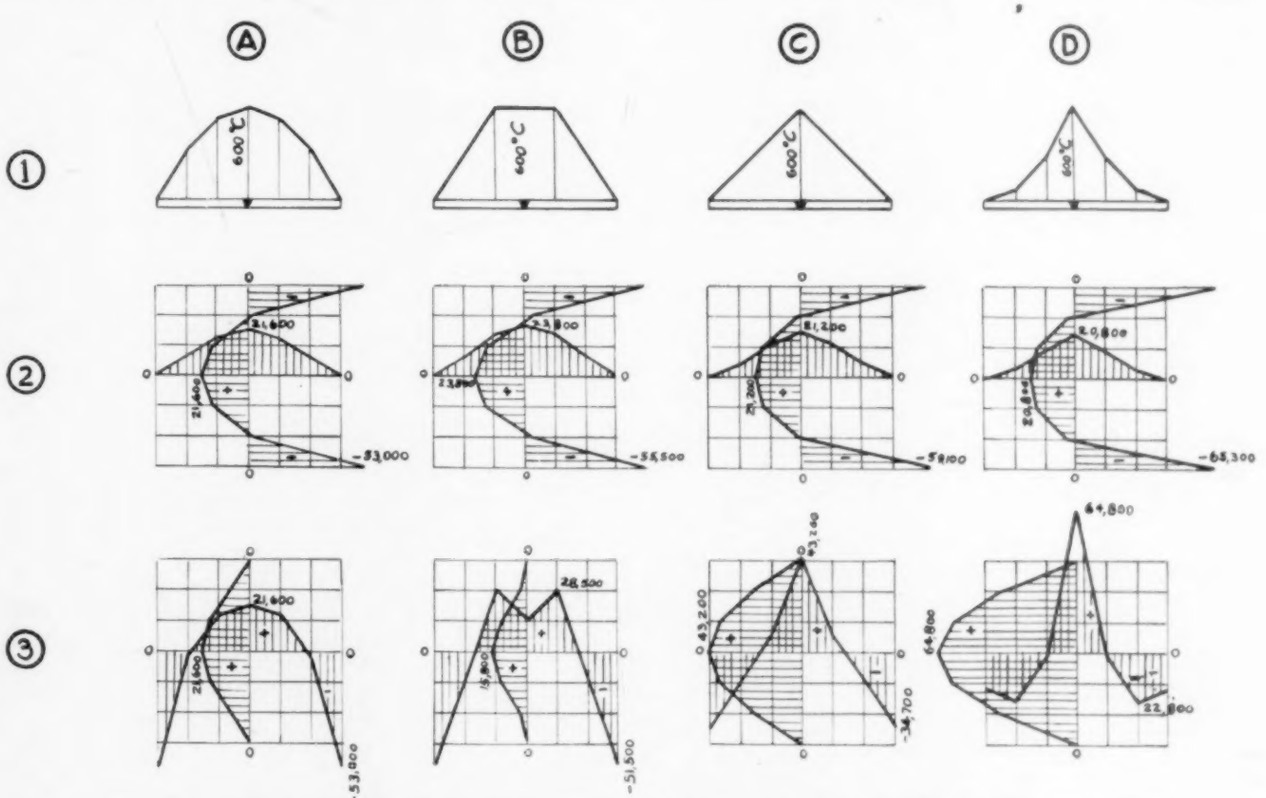


Fig. 62—Residual Stresses in Butt Welds in Mild Steel Calculated by Gruning.[149] Four Assumed Types of Temperature Distribution Are Considered

A—$\frac{\Delta^2 t}{\Delta x^2}$ = constant
t = temperature
x = distance along plate perpendicular to weld
B—large heat input
C—medium heat input
D—small heat input

Temperature is assumed constant in the direction of the weld. Charts in row 2 represent residual stresses perpendicular to the weld. Charts in row 3 represent residual stresses parallel to the weld

expression for the deflection of a rigidly gripped plate. The thermal stresses in the X direction are directly proportional to the corresponding deflections in the Y-direction in the loaded plate. The method of difference equations is employed to calculate the deflections of the plate under four assumed temperature distributions, Fig. 62. The thermal stresses are calculated from the deflections by application of the constant of proportionality between the Airy function and the deflection. The thermal stresses shown in Fig. 62, strictly speaking, are residual stresses, because the heated plate was assumed to be stress free. The calculated distribution of residual stresses is in excellent agreement with Gehring's,[21] and Bierett and Grüning's[4] experimental results with oxy-acetylene and arc-welded butt joints in mild steel, corresponding to cases (*b*) and (*d*), respectively.

A highly approximate method for calculating residual stresses with neglect of inelastic action has been developed by Fridlender.[150] The calculated stress is in terms of a constant having the properties of the modulus of elasticity of the weld in flexure. The residual stress is a function of the ratio between the distance along weld to the point under consideration and the total length of the weld.

Butt-Welded Circular Patch.—The residual stresses in a circular patch butt welded in the central portion of a disk have been studied theoretically and experimentally by Den Hollander.[151] He endeavored to find the boundary beyond which inelastic action did not occur in welding a circular patch in a mild steel plate. Experiment on a patch 8 inches diameter welded into a rectangular plate of mild steel (thickness not stated) showed that strain lines in the vicinity of the weld were in the form of a logarithmic spiral with origin at center of patch. The welds were 60° V, 0.08 inch root spacing, made with covered electrodes (yield point 4,400 psi, tensile strength

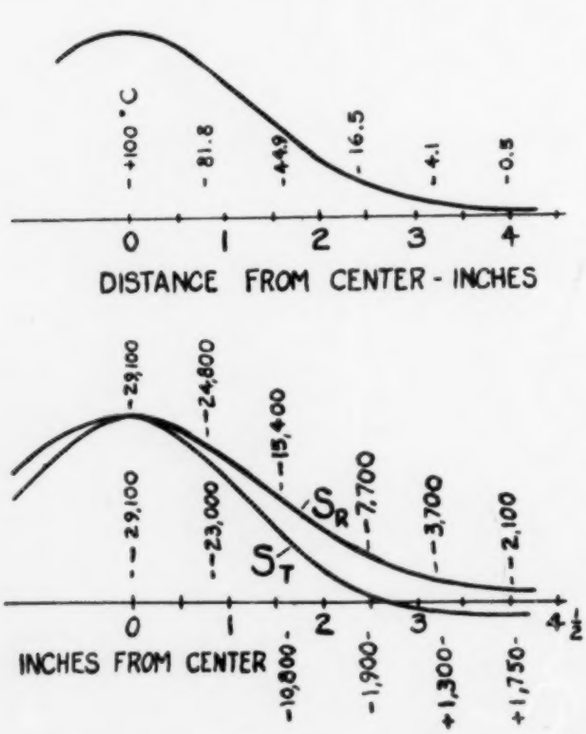


Fig. 63—Distribution of Tangential (S_T) and Radial (S_R) Thermal Stress (psi) in an Infinite Thin Disk Heated to 100° C. in the Center, the Temperature Distribution Following a Frequency Curve

Upper diagram: temperature distribution, steady state, edge of disk at 0° C.
Lower diagram: Tangential (S_T) and Radial (S_R) thermal stress (psi). Palmblad[152]

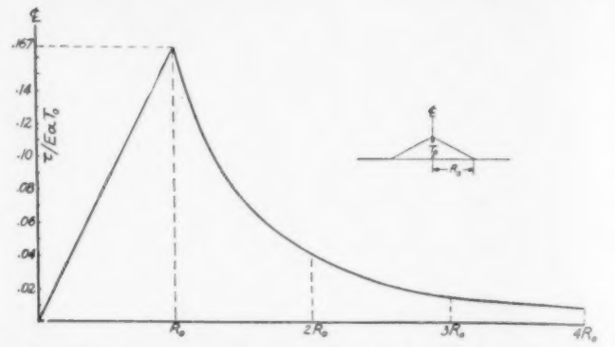


Fig. 64—Shear Stress in a Circular Plate with a Hot Spot in the Center. The Validity of the Diagram Is Independent of the Size of the Plate or the Radius of the Hot Spot. Den Hartog[148]

57,000 psi). Strain gage measurements made before and after welding on radial lines 8 to 12 inches from the weld indicated apparent stresses averaging 57,000 psi; inelastic action must have occurred.

Considering the plate with hole for patching as a thick-walled tube under internal pressure, Den Hollander wrote down the expressions:

$$S_R = P\frac{x_1^2}{x^2}; \quad S_T = -P\frac{x_1^2}{x^2}; \quad x = x_1\sqrt{P/S_R}$$

for radial and tangential stress, respectively, where P is the internal pressure, x_1 is the radius of the hole (patch), and x the radial distance to the point under consideration. Assuming that S_R is the yield point (44,000 psi) and P is the average shrinkage stress (57,000 psi), $x = 1.15\ x_1$. In other words, inelastic action occurs within the region $x = x_1$, and $x = 1.15\ x_1$. The calculation is admittedly inexact.

Spot Weld.—Calculation of the distribution of thermal stress in an infinite disk created by a central point source of heat has been made by Palmblad,[152] using Föppl's equation for radial and tangential stress in figures of rotation. The components of thermal stress are

$$\text{Radial stress} = -\frac{2G(m+1)\alpha}{2m-1}\left(t + \frac{t_0 - t}{2hr^2}\right)$$

$$\text{Tangential stress} = -\frac{2G(m+1)\alpha}{2m-1}\left(t - \frac{t_0 - t}{2hr^2}\right)$$

where G = shear modulus:

m = reciprocal of Poisson's ratio = $\frac{10}{3}$

α = thermal coefficient of linear expansion = 0.000011;

t = temperature at point distant r from the point source of heat;

h_0 = temperature at origin, $r = o$

h = a constant assumed to be 0.05 Cm^{-2}.

The system of thermal stress distribution in the infinite, thin disk is shown in Fig. 63, for a temperature rise of 100° C. at $r = o$, and a temperature distribution following a frequency curve. Thermal stresses as may have been predicted, are compressive everywhere in the vicinity of the hot spot.

Den Hartog[148] (Timoshenko) has also calculated thermal stress in a plate containing a circular hot spot. Unlike Palmblad's case, all portions of the plate beyond a small zone in the vicinity of the hot spot are assumed to remain at uniform temperature. In the small heated zone the rise in temperature to a maximum at the center

is assumed to be linear, that is, inversely proportional to the distance from the center. The radial and tangential stresses correspond to a shear stress whose distribution is shown in Fig. 64, and is independent of the size of the plate. The maximum shear stress occurs at the edge of the temperature rise and is $E\alpha T/6$ where T is the maximum temperature rise at the center. The shear stress is independent of the temperature gradient.

If the hot spot in an otherwise cold, thin plate is assumed to be small, circular, and of uniform temperature above the rest of the plate, Goodier[153] has shown that

$$S_R = S_T = -{}^1/_2\, E\alpha T \quad \text{inside the hot spot}$$

$$\left.\begin{aligned} S_R &= -{}^1/_2 \frac{a^2}{r^2} E\alpha T \\ S_T &= {}^1/_2 \frac{a^2}{r^2} E\alpha T \end{aligned}\right\} \text{outside the hot spot}$$

where a is the radius of the hot spot and r is the radial distance to the point under consideration. The maximum shear stress is ${}^1/_2\, E\alpha T$ and occurs at the boundary between spot and plate. The results differ from those obtained by Den Hartog or Palmblad because the temperature drop between the hot spot and cold plate is sudden. The three sets of equations show that a sudden temperature gradient increases thermal stress around the spot.

Goodier[153] has also shown that if the hot spot is elliptical the maximum circumferential thermal tensile stress is $E\alpha T$, twice as large as for the circular hot spot. If the hot spot is rectangular, the sharp corners cause theoretically infinite local shear stress, although the normal stress does not exceed ${}^1/_2\, E\alpha T$.

Straight Circular Tubes—Ends Butt Welded.—The thermal stress in the vicinity of a butt weld between two tubes of finite length has been calculated by Den Hartog.[148] If the temperature rises sinusoidally T degrees from a minimum at the ends to a maximum at the weld in the center, Fig. 63, the maximum thermal shear stress is shown by curve B, Fig. 66. The length, thickness and internal radius of the tube are l, t and R, respectively. Poisson's ratio is ${}^1/_{3.5}$. The curve is made up of two branches. Branch 1 represents the thermal shear stress at the center of the tube and branch 2 the stress at the free ends. Only for short tubes is the correction for finiteness important, in which case branch 2 supplies the maximum stress. The thermal shear stress in a tube of infinite length with a butt weld, Fig. 67 (half sine wave type of temperature distribution) in the center is shown by curve C, Fig. 66.

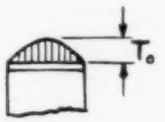


Fig. 65—Tube of Finite Length with Temperature Rising Sinusoidally from a Minimum (to Above Minimum) at the Middle of the Length. Den Hartog[148]

Using the same general method as Den Hartog (Timoshenko and Lessells, Applied Elasticity, pages 134 et seq.), Paton and coworkers[14] arrived at an expression for the residual stresses in butt-welded tubes. The derivation is based on experimental value of the "relative linear shrinkage" of a butt weld perpendicular to the seam in 0.24-inch mild steel plate (see Experimental section on Welds in Cylinders).

Straight Circular Tubes—Axial Seam.—It is shown by Den Hartog[148] that the thermal stresses created in a tube by an axial seam (uniform rise of temperature throughout the thickness of the tube in the heated band) are the same, within 1%, as those in a butt-welded joint in flat plates, Fig. 61. If the inside of the thin tube is cold, the outside T degrees maximum above the inside within the heated zone, and the rest of the tube is cold, Goodier[153] found that the axial stress within the heated zone is zero on the internal surface of the tube and $E\alpha T$ (compressive) on the outside. Elsewhere the longitudinal thermal stress is zero except for the local stress at the ends.

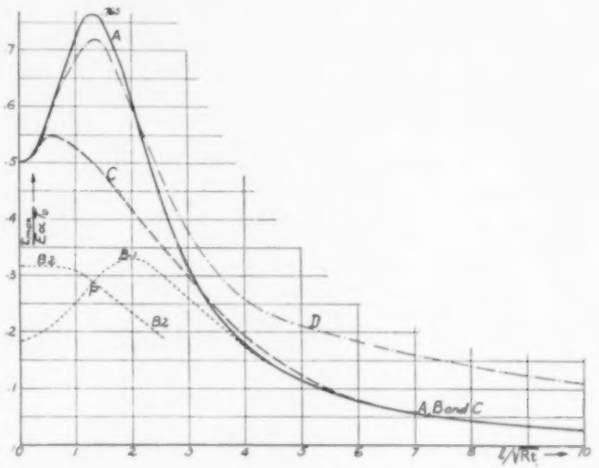


Fig. 66.—Maximum Stress in Tubes with Central Hot Spot. Figs. 65 and 67. Den Hartog[148]

Paton and coworkers[14] analyze the axial residual stress due to welding an axial seam on the assumption that for an infinitely long tube the bending caused by shrinkage of the axial seam obeys the theory of bending of tubes. Residual axial tensile stress occurs in the seam and its immediate vicinity. The "relative linear shrinkage" of a butt weld again is the basis of the formulas. The residual circumferential stresses were not analyzed. Paton and coworkers use similar methods to obtain expressions for residual stresses due to fillet welding a flat plate over the end of a drum.

Surfacing.—In the surfacing of locomotive wheels by automatic arc welding, there are, according to Sandelowsky:[184] (1) shrinkage stresses which are present when the rim is shrunk on the axle, (2) thermal stresses caused by the irregular distribution of temperature in the wheel during welding, and (3) welding stresses created by the shrinkage of the bead of surface weld metal. The maximum stress produced during welding by these three factors is obtained by summing them. The stresses due to the shrink fit are calculated by standard formulas. So also are the thermal stresses caused by the distribution of welding temperature, which was experimentally measured by means of thermocouples at ten locations in the cross section of the wheel. The welding stress is computed by means of the formula

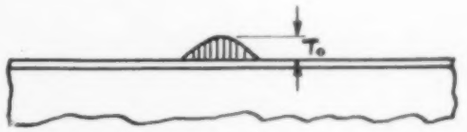


Fig. 67—Tube of Infinite Length with Hot Spot of Length l_1. The Temperature of the Tube Rises Sinusoidally from a Minimum at the Edge of the Hot Spot to a Maximum (T_o Degrees Above Minimum) at the Center of the Hot Spot. Den Hartog[148]

$$S = E\alpha\,[600 - (T_b + T_w)]$$

where T_b = temperature of bead = 1500° C., and
T_w = temperature of wheel at cross section for which total stress is being computed = 160° C.

The temperature at which steel becomes elastic is assumed to be 600° C. A correction factor is applied to the formula to account for heat of transformation (no details).

The Theory Involving Inelastic Deformation

Despite the large amount of experimental investigation that has been devoted to the measurement of the distribution of residual stresses in welded joints, there is no quantitative theory taking into account inelastic deformation. Qualitatively, it is well realized that the hot part of a welded butt joint, for example, is usually inelastically compressed at some stage during welding. During cooling the permanently shortened zone endeavors to return to its original dimensions and is consequently stressed in tension. Experiment has demonstrated that the tensile elongation usually corresponds to a tensile stress beyond the elastic limit or yield point on the stress-strain curve. To secure equilibrium in the free, welded plate, zones adjacent to the tensile region must be subjected to residual tensile stress.

The qualitative theory, which has been particularly well expressed by Bierett[39] and Rosenthal[145] has been amplified by Leykin[155] to take into account the rapid decrease in elastic limit of steel as the temperature is raised. The temperature at which mild steel appears to lose all elastic characteristics, independent as far as possible of the time effect, has been estimated to be 300 to 650° C. by different investigators. However, all the discussion on inelastic deformation as a factor in residual stresses in welded joints has been strictly qualitative. On the other hand, there is a well-developed theory for predicting stress and strains parallel to the welded edges of an edge-welded plate at all times during welding.

This theory is due to Boulton and Lance Martin[62] (see section on Beads Deposited on Edge of Plate), and, on account of its uniqueness, it is summarized at length in the following paragraphs. Considering the single edge welded plate, these investigators showed that the experimental results of linear variation of longitudinal residual shrinkage distortion across the width of the plate led to the stress distribution shown by line *m n o*, Fig. 68, if inelastic action was neglected. Since the welded plate was not constrained during or after welding it was necessary that two equilibrium conditions be satisfied that were previously used by Bierett and others, namely, the distribution of residual stress parallel to the welded edge across any section perpendicular to the edge must be statically equivalent to zero force and zero moment To fulfill the two equilibrium conditions the distribution of residual stress could not be that given by line *m n o*. It was therefore assumed that the compensating residual stress could be provided by a rectangular area in the tensile half of the diagram. The dimensions of the rectangle were calculated by means of the two equilibrium conditions, the resulting distribution being line *m n p q*.

The sudden change at x_0 from tension to compression is not plausible and further investigation led to a modification in the shape of line *n p*. The so-called rectangular distribution did show, however, that the maximum residual tensile stress between x and x_0 cannot be less than the value H_0 provided by the rectangular distribution, and that the width of the plate that was inelastically

Fig. 68—Hypothetical Stress Distribution Across a Single-Edge-Welded Plate

Line *m n o* is Fictitious Distribution of Stress Parallel to Welded Edge Calculated Directly from Extensometer Measurements Before and After Welding. This Distribution Is Fictitious Because the Two Conditions of Equilibrium Are Not Fulfilled: Namely, the Total Compressive Stress in the Free Plate Must Equal the Total Tension, and the Moment of the Cross Sections Under Tensile Stress Calculated About the Welded Edge, Must Equal the Moment of the Cross Sections Under Compressive Stress. The Fictitious Distribution Fails to Fulfill These Equilibrium Conditions. An Area of Tensile Stress Is Required to Replace Some of the Excess Compressive Stress. The Only Section at Which the Replacement Can Be Made Is the Section Near the Welded Edge Which Underwent Inelastic Action During Welding. The Simplest Way of Satisfying Equilibrium Conditions Is to Replace Some of the Compression Area with a Rectangular Area of Tensile Stress. The Only Area of This Sort That Satisfies the Equilibrium Conditions Is Shown by H_0X_0. Boulton and Lance Martin[62]

strained during welding and subsequent cooling must have equalled or exceeded x_0. It should be observed that the rectangular solution presupposes that the stress-strain curve of mild steel at temperatures up to 500° C. consists of a straight elastic portion and a portion parallel to the strain axis. The stress-strain curve of mild steel actually approximates the assumed curve rather closely for unit elongations not exceeding 1% or so. Under this assumption, which was used throughout the remainder of the study, the plate between $x = o$ and $x = x_0$ is at the yield point (or elastic limit) and remains at that stress even during additional elongation. If the stress-strain curve did not contain a yield point the residual stress distribution satisfying the equilibrium conditions follows an empirical curve, such as that drawn dotted in Fig. 68.

To calculate the position and shape of line *n o* under more plausible conditions than those assumed in the rectangular distribution, Boulton and Lance Martin quantitatively studied the variation of yield point with temperature and time in all regions of the plate that reached the yield point at any time during welding. The time-temperature relationship desired by Boulton and Lance Martin for temperature distribution in an edge-welded plate during welding was discussed in the review on Temperature Distribution in Welding. The stress-temperature relationship was assumed to be that found by F. C. Lea (1914) in his well-known short-time tensile tests of steel at high temperature. It was assumed in view of the short time involved in welding, that stress-time relationships could be neglected, and that Lea's results for tensile stress applied to compressive stress as well. Further, the assumption was made that the tensile yield point at any temperature is not lowered by previous occurrence of compressive yield.

To state the problem again, it was necessary to find the residual stress distribution in three regions of the plate: (1) the region which remained below the yield point at all times; (2) the region adjacent to the welded edge which, as in the rectangular distribution, was strained beyond the yield point. This region, therefore, is in a state of constant residual stress; (3) the intermediate region in which at some time and temperature during welding the

yield point was exceeded, but in which during cooling the stress fell below the yield point. In region 1 the stress at all time is given by the strain parallel to the welded edge multiplied by the Young's Modulus for the temperature prevailing at the point under consideration. In region 2 adjacent to and including the welded edge, some of the steel for some periods of time during welding is heated to or close to the melting point. The capacity for resisting stress is practically zero. Upon cooling to room temperature, however, as mentioned before, this region, whose width depends on the instant during welding that is under consideration, assumes a state of constant stress. The width of region 3 also varies with time, but its boundary on the elastic side remains constant after the instant of maximum penetration, t_1. In other words, inelastic deformation occurs only between the welded edge and x_1, which is the plane at which the longitudinal stress just reaches the yield point at t_1.

In all regions at all times stress is calculated from deformation. It was found experimentally that residual deformation varied linearly with distance from the welded edge. The assumption was, therefore, felt justified that at any time during welding, deformation parallel to the welded edge varied linearly with respect to x across the whole width of the plate, except in cross sections near its ends. The deformation was considered to be the average value over the thickness, and deformation perpendicular to the welded edge was neglected. The last restriction considerably limits the scope of the theory because it is well known that distortion across a welded joint is anything but insignificant. The experiments of Lance Martin[18] (see section on Experimental Results—Edge-Welded Plate) showed that for single-edge-welded plates the deformation was, in fact, negligible perpendicular to the welded edge at sections not too near the weld, in so far as these transverse deformations were not created by stress parallel to the edge.

At any point the deformation parallel to the weld is the sum of two parts:

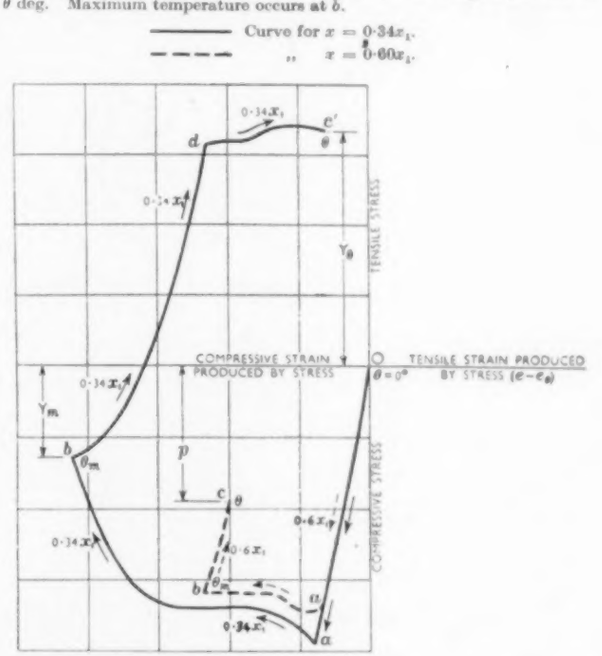
1. Deformation due to temperature rise (αT per unit length, where α is the coefficient of linear thermal expansion at the temperature under consideration, and T is the rise in temperature above initial);

2. Deformation accompanying stress (S/E_T in the elastic region, where E_T is Young's Modulus at the temperature T, and S is stress parallel to the weld). The assumption of linear variation of deformation across the width of the plate provides the key to a simple equation for calculating the stress at any point in the elastic region 1. The stress and deformation in the inelastic regions are considered to vary during the complete welding process as shown diagrammatically in Fig. 69 for two sections. In all parts of the inelastic regions the variation of stress with strain produced by stress follows curve Oa (curved because E varies with temperature) until the compressive yield point is reached. Inelastic yielding at yield point stress then occurs, curve $a\ b$, until the maximum temperature for the section is reached. The maximum temperature is calculated from the temperature distribution equation. The compressive yield point corresponding to the maximum temperature is obtained directly from Lea's curve of tensile yield as a function of temperature.

It is assumed, without experimental basis, that the maximum compressive deformation occurs at the instant the maximum temperature is attained. As the temperature falls thereafter, the stress at section $x = x_1$ and further from the welded edge simply retraces curve Oa for a short distance. For a section somewhat closer to the edge: $x = 0.60\ x_1$, the stress follows a curve parallel to Oa for some distance as shown partly by the dotted curve in Fig. 67. The stress in sections still closer to the welded edge, such as $x = 0.34\ x_1$, also follows an elastic curve parallel to Oa until the yield point in tension is reached. Inelastic deformation then occurs until the section reaches the initial temperature of the unwelded plate. In Fig. 69 the temperature at the two sections is not the same at instant t_1. Further the compressive strain produced by stress does not include temperature deformation. Consequently, the strains at instant t_1 shown in Fig. 69 need not fulfill the assumption of linearity of deformation across the width of the plate at any instant. Boulton and Lance Martin do not analyze the case in which inelastic action occurs at all points across the width of the plate.

Using the two equilibrium conditions, Boulton and Lance Martin originate a method of calculating deformation and stress parallel to the welded edge at all sections across the width of the plate both for the instant t_1 of maximum penetration of the region of inelastic deformation as well as for the time the plate has reached uniform initial temperature after cooling is complete. As may be inferred from the preceding paragraph, the solution is by trial. The residual stress requires a determination of deformation and stress at instant t_1. The temperature distribution curve provides values of temperature at t_1, from which the temperature deformations at all sections are calculated. The temperature deformations give the curvature in the elastic region to the stress distribution curve, Fig. 36, which otherwise would be a straight line. A process of graphical integration is used

Points c, c' are reached at the instant t_1, when the temperature rise is θ deg. Maximum temperature occurs at b.



Part of curve	Strain	Temperature
O to a	Elastic	Rising
a to b	Plastic	,,
b to c	Elastic	Falling
b to d	,,	,,
d to c'	Plastic	,,

Fig. 69—Stress Variation at Two Points in the Region Undergoing Inelastic Deformation During Welding of an Edge-Welded Plate

X_1 = distance from welded edge to extreme limit of region undergoing inelastic deformation during welding. Boulton and Lance Martin[62]

to determine the area under the plot of x *vs.* αT, which is used in calculating the contribution of temperature deformation e_T to equilibrium. Trial values of x_1 are then inserted in the equation for deformation at x_1 until the trial value of deformation e_1 so calculated leads to the value of yield point stress Y_T indicated by Lea's curve for the temperature at t_1

$$Y_T = E_T(e_1 - e_T);\ x = x_1$$

When, by repeated trials, the correct values of x_1 and, hence, of e_1 have been found the stresses across the width of the plate at instant t_1 may be calculated by simple substitution in other formulas.

The numerical results for stress distribution in a single edge-welded plate calculated by this method are shown in Fig. 36. The curve fulfills the two conditions of equilibrium. The curvature in the elastic regions is supplied by the temperature deformations, not by the change in Young's Modulus with temperature, an average modulus being used in the elastic region. Strictly speaking the horizontal part of curve 1, Fig. 36, should be a curve rising from the value of tensile yield stress at 375° C., which is the temperature of the welded edge at instant t_1 predicted by the equation for temperature distribution, to the value at 240° C., the temperature at x_1 at instant t_1. However, Lea's curve shows no appreciable difference in yield point stress in the range 240 to 375° C. Boulton and Lance Martin do not state the effect of a steep temperature gradient combined with a high edge temperature on the calculations. On the other hand, the solution may be obtained for any arbitrary distribution of temperature.

Having determined the boundary of the region that remained elastic at all times, Boulton and Lance Martin determined the residual stress distribution, that is the distribution after the plate had cooled uniformly to the initial temperature by a process of trial similar to that outlined for the distribution at instant t_1. The tensile yield stress at room temperature is used instead of that at instant t_1 for $x = x_1$. The numerical results for the single-edge welded plate are shown in Fig. 36. The distribution is linear in the elastic region because the temperature is uniform. The position of x_1 has not changed from that at instant t_1 because at t_1 by definition the boundary of the region that underwent inelastic deformation at any time had penetrated deepest into the plate. The maximum residual compressive stress is slightly less than that at instant t_1, but the maximum residual tensile stress is slightly higher corresponding to the rise of yield point as the temperature is lowered.

There is no difficulty in applying the theoretical solution to a plate welded simultaneously in the same direction on both edges. The temperature stress and strain distributions are symmetrical about the center line of the edge-welded plate. The result of superposing two linear deformation distributions of the type discussed in the single-edge-welded plate is a constant compressive deformation across the entire width of the plate. Experiments by Lance Martin showed that the theoretical distribution for the single-edge welded plate is more closely realized than for the double-edge welded plate.

The numerical results calculated for the double-edge welded plate are shown in Fig. 37. The width of the inelastically deformed region was somewhat less, and the residual stress gradient somewhat greater, than in the single-edge welded plate because the welding speed was twice as great in the double-edge welded plate as in the single. Calculation showed that if the width of the double-edge welded plate was increased to 10.5 inches, x_1 was increased 0.19 inch and the constant residual strain was decreased by nearly 15%.

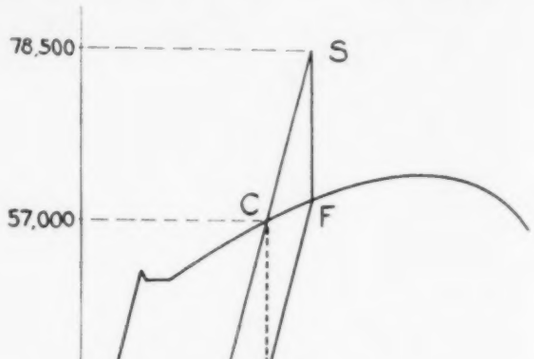


Fig. 70—Effect of Overstrain in Reducing Residual Stress in Mild Steel Welds. Wörtmann and Mohr[156]

It must be said that Boulton and Lance Martin's theoretical solution of the problem of shrinkage stresses in edge-welded plates using a theory involving inelastic as well as elastic deformations is a decided advance over the solution indicated by the Elastic Theory. The Elastic Theory predicts no thermal stress whatever in a plate heated uniformly on one edge if the temperature gradient back of the edge is linear across the width. Thermal stress would be predicted by the Elastic Theory for a plate heated uniformly on both edges, but no stresses would remain in the plate upon return to the initial uniform temperature. Boulton and Lance Martin believe that their solution will apply approximately to multi-layer welds, and that clamping the edges of the plate during welding would increase the width of the region of inelastic deformation.

Theory of Overstrain in Welded Joints

The relief of residual stress in welded joints by overstrain has been discussed in the Experimental Section, where it was made clear that the first regions of the joint to be permanently deformed were those in which the residual stresses were highest in the direction of overstrain. As in the theory of overstrain of other materials, the permanent deformation caused by overstrain in the welded joint equalizes the elastic deformations which, on unloading, disappear to some extent, thus relieving the residual stress.

Wörtmann and Mohr,[156] Ziem,[157] Buchholz[109] and Harrison[158] have illustrated the nature of the relief by means of the same spring model. In Harrison's model two pieces of wood are joined by a spring balance representing the weld. The combination is suspended vertically and a second spring balance is attached to the lower piece of wood. Both balances are adjusted to read zero. A downward pull on the lower balance then produces the same reading on both balances. A residual stress is now placed in the "weld" by means of a pair of vertical screws and wing nuts acting on cross arms. If the residual stress is recorded as, say, 10 lb., a downward pull of up to 10 lb. can be applied to the lower balance without changing the reading of the "weld" balance. Above 10 lb. both balances record the same load.

Wörtmann and Mohr[156] also relate overstrain to the stress-strain diagram, Fig. 70. A longitudinal welded

joint having residual stress up to 57,000 psi is loaded externally on additional 21,500 psi in the direction of the residual stress. The stress in the weld changes from C to F. Point F is determined by prolonging CE to S = 78,500 psi and drawing SF perpendicular to the axis of deformation. Upon removal of the external stress, the residual stress is decreased to K, which is the intersection of CD with FJ (parallel to CE). Wörtmann and Mohr's theoretical explanation of relief by overstrain is highly approximate.

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Appendix—Methods of Measuring Residual Stress

(NOTE: *It is expected that more detailed discussions of some of the different methods of measuring shrinkage stresses in welds will be prepared in the future.*)

Subdivision Method

Residual stresses in metals caused by welding, or any other process, are usually measured by means of the "subdivision" method, of which there are many variations. The "subdivision" method commonly used for welding stresses consists of marking gage points a known distance apart on lines parallel and perpendicular, or radially and tangentially, with respect to the completed weld. A common gage length is 2 inches. The distance between the gage points may be measured by means of any sensitive measuring instrument, such as a micrometer, extensometer or comparator. The metal containing a pair of gage points is then removed from the welded part by machining. All forces acting on this strip of metal due to its surroundings having thus been destroyed, the strip becomes essentially free from stresses. The distance between gage points is again measured. If the distance has increased then the gage length must have been compressed by its original surroundings in the direction of the line connecting the points. If the distance after cutting is found to be less than the distance before cutting, there must have been a net tensile stress acting along the line between the gage points. The net strain is the average over the gage length and is considered as acting at the midpoint of the gage length. As the gage length is increased, or as the stress changes more rapidly due to welding conditions, the average strain thus measured becomes less and less significant. In another similar method the metal between the gage points is cut out. If the distance between gage points increases after the intervening metal is removed, the metal must have been under initial tensile stress.

To calculate the average stress at the mid-point of the gage length from the observed unit strain requires a knowledge of what kind of stress condition is being studied. If it is believed that the only direct stress acting on the gage length was in the direction of the line joining the gage points, then the average stress is

$$S = E.x,$$

where E = Young's Modulus, psi
and x = unit strain, inches per inch.
If, on the other hand, the only direct stress acting on the strip is known to act at right angles to the line joining the gage points, then the average stress is

$$S = \frac{E.x}{u}$$

where u = Poisson's Ratio.
In the former case, the calculated stress acted along the line joining the gage points, in the latter case, the calculated stress acts at right angle thereto.

In the usual welded joint, as is well known, there are strains both parallel and perpendicular to the joint, and it may readily be conceived that the residual stresses will also be of two kinds: parallel and perpendicular to the joint. With this assumption, which is probably justified for simple joints, it is sufficient to measure the residual strains along two gage lengths parallel and perpendicular, respectively, to the weld, and intersecting at their midpoints. The average stresses acting at the mid-point are then:

$$S_1 = \frac{E(x_1 + ux_2)}{1 - u^2}$$

$$S_2 = \frac{E(x_2 + ux_1)}{1 - u^2}$$

where S_1 and x, are the principal stress and strain, respectively, parallel to the weld;
S_x and x_2 are the principal stress and strain, respectively, perpendicular to the weld.

In case the directions of the principal stresses are not known, then three gage lines with common midpoints and at 60° to each other are used (graphical method—Osgood, W. R. & Sturm, R. G., *Bureau of Standards, Journal of Research*, **10**, 685–692 (1933), or four gage lines at 45° (numerical method—Osgood, W. R., *Bureau of Standards, Journal of Research*, **15**, 579–581 (1935)).

Since the gage points in the subdivision methods are always on or close to the surface, the results are in terms of surface stresses. For thin plate, up to say $^1/_2$ inch, the variation of residual stress across the thickness may be neglected. For thick plate, however, the third principal stress, in the direction of the thickness of the weld becomes important. The subdividing cuts necessary to determine the third principal stress are then along planes parallel to the plane of the plate.

Another variation of the subdivision method involves

successively cutting away portions of the specimen and observing the consequent changes in length of gage lines on the remaining portions of the sample. When the portions cut away are simply drilled out of the center of a disk with, for example, a central patch welded in, the changes in diameter of the remaining ring are measured and the method is known as the Sachs Method. Radial and tangential stresses are determined from the changes in diameter.

A method of determining residual stresses in welds that is NOT SAFE is the "direct" method in which measurement is made of the change caused by welding in the distance between two gage points on a plate. In the absence of subdivision, the direct method is misleading.

Mathar Method

The underlying principle of the Mathar Method is the same as that of the Subdivision Method: namely, the release of residual stress by removal of some of the material carrying the stress. The stress is relieved in the Mathar Method by means of a special drill $^1/_2$ inch in diameter. The stress originally supported by the material of the hole deforms the hole and the material in the immediate vicinity. The deformation is measured by means of an extensometer, 6 inch gage length. The extensometer readings are converted to stress by means of a calibration secured on a specimen under known loads.

The advantage of the Mathar Method is that the hole is relatively small and the stress measurement is correspondingly localized, rather than average. For this reason, individual measurements of residual stresses determined by the Mathar Method, and also by the X-ray method, are likely to give higher results than the subdivision method. Among possible sources of error, the Mathar hole may deform inelastically particularly in bi-axial stress fields, under the action of stress concentrations around its periphery. The effect is not great, however, and may occur in the subdivision method, as well.

X-ray Method

The X-ray Method of measuring residual stresses is direct. The lattice parameter of the specimen to be measured is determined in one or more directions by precision methods. Under elastic stress the lattice expands or contracts depending on the stress applied, a cubic lattice, in general, becoming anisotropic. The changes in lattice dimension with respect to the unstrained material are the basis of residual stress calculations.

The advantages of the method are that the stress is measured over a much smaller area than is usually possible by any other method, and the specimen need not be cut to bits. Disadvantages are that the stress in a thin surface layer only is determined (less than 0.001 inch deep) and that structural and inelastic changes that often occur in welded joints greatly reduce the sensitivity of the method. Norton and Notvest showed the effect, respectively, of residual "strains" in welds in aluminum and steel on asterism, and of residual stresses in steel welds on line broadening in Laue patterns. Unsatisfactory results were obtained by Crousaz, Haigh and Pullin, who applied the back-reflection method with K α Cobalt doublet to $^1/_4$-inch slices cut from weld metal deposited by covered electrodes. Although there was no apparent variation in lattice parameter (accuracy 1 in 20,000), it was felt that the presence of residual stresses and inelastic deformation was not excluded.

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Suggested Fundamental Research Problems on Shrinkage Stresses in Welding

1. Magnitude and distribution of shrinkage and reaction stresses in spot- and seam-welded assemblies, and of shrinkage stresses in flash welds and resistance butt welds.

2. Shrinkage stresses in welded high-alloy steels.

3. Shrinkage stresses in welded non-ferrous metals. Do shrinkage stresses (if any) in welds in some non-ferrous metals which exhibit a measurable creep rate at room temperature disappear in the course of time? What relief or increase of shrinkage stresses may be expected by aging reactions at room temperature in some precipitation hardening non-ferrous alloys? (An annotated bibliography of articles dealing with the residual stresses set up in metals by aging or allotropic transformations is available upon request.)

4. Magnitude and distribution of shrinkage stresses perpendicular to the surface of the plate (in the direction of the thickness) in welds in plate more than about $^1/_2$ inch thick.

5. Shrinkage stresses in arc welds made in thick plates in a single pass.

6. Shrinkage stresses in soldered, bronze-welded, and thermit-welded joints.

7. Effect of shrinkage stresses (if any) on the buckling of welded rails.

8. For a given type of weld in a given material is there a relation between heat input, speed of welding (arc, gas,

flash, etc.), speed of cooling (Newton's Law), and shrinkage stresses *perpendicular* to the weld? A quantitative theory of this sort based on inelastic deformation has been evolved by Boulton and Lance Martin for shrinkage stresses *parallel* to the weld at all times before and after the source of welding heat has passed a given section.

9. A complete and analytical catalog of welding cracks with accompanying quantitative data on the rate and temperature of propagation of the crack, and the magnitude of the shrinkage stresses.

10. Thermal shock tests (plunging a locally heated body into a quenching bath) as an indication of the response of a metal to some of the factors involved in weldability without introducing the complications of weld metal or extensive microstructural changes.

11. To what extent may overstrain be relied upon to relieve shrinkage stresses in welded higher carbon steel? To what extent does impact overstrain relieve shrinkage stresses in welded low-carbon steel?

12. There is a need for a review of theoretical and experimental work on multi-axial stresses. It is believed that a cause of brittle cracks at room temperature in ductile welds is multi-axial stress, but it is not known to what extent multi-axial stresses may be expected to reduce the capacity for deformation (slide) and to account for brittle failure. (An annotated bibliography on some phases of theories of strength is available upon request.)

13. There is a large amount of information on relief of external stress on unwelded metal by stress annealing. To what extent does it apply to the relief of different systems of shrinkage stresses in welds by annealing? Are the reservations important or minor? (An annotated bibliography on the effect of annealing on relief of stress in unwelded metals is available upon request.)

14. A table containing reliable quantitative data on the physical properties (thermal and electric conductivity, vapor pressure, etc.) of metals at all temperatures that are important for all types of welding.

Brazing in a Continuous Furnace with Protective Atmosphere*

By E. KUHLMANN

THE brazing of small steel parts with copper or brass in a neutral or reducing atmosphere without flux is a well known process. For mass production of brazed parts the AEG have developed a continuous electric furnace consisting of a heating chamber joined with a water-cooled cooling chamber. The parts to be brazed are placed on an endless belt of heat resisting wire mesh. The belt is driven by rubber rollers outside the heating chamber. A gas pressure slightly above atmospheric is maintained in the furnace and cooling chamber, and excess gas burns at all openings as a protective curtain. A maximum temperature of 1150° C. may be attained in the furnace; the parts leave the cooling chamber at 100° C.

Protective Atmosphere

The protective gas is practically safe against explosions and its composition is adjusted to suit the work. A dissociated illuminating gas is used, the gas being drawn from city mains. Air and city gas in automatically metered quantities are passed through a combustion and catalyzing chamber in which partial combustion occurs. The prepared gas is cooled and dried. In special cases hydrogen sulfide is removed. The gas is generally of a reducing composition so that surface oxide on the work is reduced.

Copper wire is placed on the joint (in case copper wire cannot be applied, a paste of copper powder and varnish may be used) and, when molten, is drawn by capillary action into all parts of the exceedingly narrow space between the parts, which have been pressed together. Practically no copper remains on the exterior. The copper in the joint partly alloys with the metals to be brazed.

Mechanical Properties

Instead of a compression test, which reveals the brazed joint as stronger than it actually is, tensile tests were made on mild steel bars (0.1% C) 0.28 inch diameter made up of a number of disks 0.16 inch thick brazed together. All excess copper was removed electrolytically in a cyanide bath before testing.

Effect of Fit

Table 1 shows the tensile strength of specimens brazed for 20 minutes in the continuous furnace. The average strength is above that of pure copper (29,800 psi) and increases as the fit is made closer. Tests have shown that the maximum play for brazed joints of this type is 0.004 inch. Best properties are obtained with as thin a copper layer as possible.

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Table 1—Effect of Fit on Tensile Strength of Brazed Mild Steel

Kind of Fit	Tensile Strength, psi Maximum	Minimum	Average
Loose joint (0.004 inch play)	23,300	21,900	22,800
No play	30,700	26,900	29,200
Press fit (bolts drawn up 0.020 inch)	33,700	28,200	30,400
Press fit (bolts drawn up 0.015 inch)	32,000	29,000	30,600

Effect of Time of Brazing

Table 2 shows the effect of duration of brazing on the strength of a press fit joint (bolts drawn up 0.002 inch). The strength increases up to 30 minutes beyond which it decreases. Since the copper undoubtedly flows completely into the joint in the first few minutes, the beneficial effect of longer times can be explained only by mutual diffusion of copper and iron with consequent alloy formation. When brazing was prolonged from 45 to 60 minutes there was only a slight trace of copper visible on the surface; the remainder diffused into the iron, which explains the lower strength.

Table 2—Effect of Duration of Brazing on Tensile Strength of Mild Steel Brazed in Electric Furnace (Averages of Ten Tests)

Time in the heating chamber of the furnace, minutes	Tensile Strength, psi Maximum	Minimum	Average
4	27,900	25,800	27,200
8	29,300	27,000	28,500
15	29,900	27,600	28,600
22	32,000	28,200	30,200
30	*33,800*	*30,200*	*32,400*
45	33,000	26,400	29,600
60	32,700	24,900	29,500

Effect of Subsequent Heat Treatment

The specimens were brazed with copper, press fits, time of 20 minutes. Excess copper was removed in a cyanide bath. The parts were then hardened and the tensile strength determined. Table 3 shows that heat treatment decreases the strength of the joint. The great differences between the results for presumably identical specimens should be noted, but even the lowest value is above that obtained by riveting, spot welding, flame soldering or other methods.

Table 3—Effect of Hardening on the Strength of Electric Furnace Brazed Joints in 0.1% C Steel

Heat Treatment	Tensile Strength, psi Maximum	Minimum	Average
Water quenched after case carburizing 815° C.	28,700	13,900	22,500
Oil quenched after case carburizing 815° C.	29,300	16,400	24,600
Water quenched after 20 minutes in cyanide hardening salt bath at 815° C.	23,400	9,500	17,500
Oil quenched after 20 minutes in cyanide hardening salt bath at 815° C.	29,600	9,100	22,800
Water quenched after case carburizing at 900° C., then reheated in furnace to 775° C. and water quenched	31,900	9,800	28,900

The results of heat treating on some electric furnace brazed alloy steels are shown in Table 4. Remarkably high strengths are obtained.

Table 4—Effect of Heat Treatment on the Strength of Electric Furnace Brazed Joints in Different Steels

Steel	Temperature, ° C.	Quenched in	Drawn	Tensile Strength, psi Maximum	Minimum	Average
Plain Carbon, 0.2% C	840	water	no	49,800	41,600	44,800
Chromium-vanadium	870	oil	555° C.	40,600	26,300	35,000
Nickel-molybdenum	840	water	no	59,800	44,200	53,000
Tool Steel, 1.1% C	775	water	no	44,400	31,900	36,300

Fatigue Strength

The R. R. Moore rotating bend fatigue machine was used. The specimens were made up of disks of 0.1% C steel and were machined after having been brazed in the continuous furnace. A fatigue strength of 15,000 psi was found, but more tests should be made.

Impact Value

A repeated impact test, 800 blows a minute, was devised in which the specimen consisted of a hub with brazed sleeve. A pinned sleeve failed after 1,400,000 blows. A brazed sleeve made in the controlled atmosphere furnace showed no sign of failure after 5,300,000 blows, when the test was discontinued.

Advantages

The advantages of joints brazed in a controlled atmosphere are:

1. Saving in space, weight and material. No pins or rivets are required, and flanges may be made smaller.
2. Increase in wear resistance by hardening after brazing. Riveted parts must be soft so that the hole may be drilled.
3. Increased strength. A list of twelve parts is given in which the part brazed in a continuous, controlled atmosphere furnace had 52 to 1014% greater strength than the pinned or riveted part.

A New German Thermit Welded Rail Joint For High-Grade Rails*

By W. AHLERT

THE new method is illustrated in Fig. 1 and is intended for rails of simple shape having a tensile strength over 120,000 psi and up to 185,000 psi. The principle of the method consists in raising the temperature of the rail ends in contact above the solidus into the mushy region of liquid and solid. The rail heads weld together but do not lose their shape because a sand cap, Fig. 1, is fitted over them. The heat is provided in the

* Translated by Dr. G. E. Claussen, Research Assistant. Published in *Organ für die Fortschritte des Eisenbahnwesens*, Berlin, Vol. 92, Pages 222 to 225, June 15, 1937.

usual way by Thermit. The method is particularly suited to high strength steels, for the pressure welding of these steels below the solidus is difficult and there is a wide range of temperature between liquidus and solidus.

The heads of the rails to be joined are fitted closely together. A gap of about $^1/_4$ inch is cut in the foot and web. If for any reason this gap cannot be cut in the rails, a piece of rail steel may be inserted between the heads. The insert requires two welds to be effected and should not be used unless absolutely necessary, because the two parts to be welded are slightly outside the center line where maximum heat is developed. In other respects the joint is prepared exactly the same as in the usual "combined Thermit process." A mold with opening at the top is placed around the joint, which is then preheated to an orange or white heat at the top. When the desired preheating temperature is reached, a sand cap is placed over the head of the rails.

Immediately thereafter the Thermit is poured. The slag rising to the top of the mold solidifies as a crust on the relatively cool sides of the cap. Thus, the rail head is supported on all sides and no distortion occurs. Experiments showed that without the cap the slag washed away part of the rail head.

Shortly after being poured, the joint is pushed up 0.12 to 0.16 inch. In contrast with the usual combined process the push up requires no power because the rail head is in a mushy condition. The push up in the new process has the effect of correcting any irregularities of shape, and has no pressure welding effect as in the usual Thermit pressure process.

Microscopic examination shows very little change in structure but complete welding of the heads. Although the rolled structure is somewhat changed there is no indication of cast structure except in the Thermit weld. The heads are pearlitic, containing perhaps 0.8% C. The Thermit weld metal is an alloy of Thermit iron and rail steel and contains 0.3 to 0.4% C. The physical properties of the Thermit weld metal may be improved by alloys.

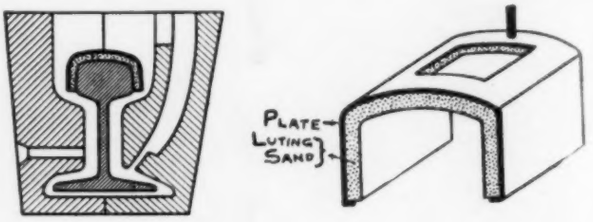

Fig. 1—Cap Placed Over Rail Head After Preheating to Orange or White Heat

A Brinell hardness exploration along the top of the welded rail head showed that hardness of the joint and its vicinity was about 300 Brinell. Unwelded rail had a hardness of 280 Brinell. The effect of welding on hardness disappeared at a distance of $3^1/_8$ inches from the center of the joint. The effect of the increase in hardness at the joint on the behavior of the rail in service has not yet been studied.

Bend-deflection tests on the welded joints are quite satisfactory. The span was 40 inches, the head of the rail being in tension. The average extreme fiber stress was over 140,000 psi; the deflection at the middle was over 0.63 inch. Welds in duplex rail withstood an extreme fiber stress of 115,000 psi at a deflection of $1^3/_{16}$ inches. Many rails after the bend test withstood the impact of a 220-lb. weight falling from a height of 20 feet without failure.

(*Continued from page 1*)

continue his research work. And research work in engineering, especially if it is experimental work, is expensive when considered as an item in a university budget. However, the technical findings of much of this research work is of value to industry. Of even greater importance, industry profits by the services of the young men whose advance training depends so much on the research work of the professor. The bulletins of the University of Illinois Engineering Experiment Station are known throughout the engineering world for their contributions to engineering knowledge. But to our college of engineering, the bulletins are a by-product, a valuable one, but a by-product, nevertheless. Our real product is the young men whose advance training has been made possible through the research work that has resulted in the bulletins. There is one company that has been a particularly liberal contributor to our research work. The method of operating has been for the company to provide the funds with which to pay the direct expenses of investigations of interest to it. The university provides library and laboratory facilities and contributes the services of a professor to supervise the investigation. The actual work is done by half-time graduate research assistants. These men are selected from the graduates of engineering schools all over the United States, men whose undergraduate records indicate that they have an intellectual capacity that makes them worthy of further training. They are appointed for two years and they devote half of their time to a research project, working under the supervision of a professor. The remainder of the time is devoted to organized graduate courses. At the end of two years they receive the degree of master of science in civil engineering. If the result of their work has sufficient value it is published as a bulletin of the Engineering Experiment Station, the cover page indicating that the bulletin has resulted from a cooperative agreement between the university and the company. This company has financed investigations that have resulted in four of these cooperative bulletins. I am sure that the company is satisfied that the technical knowledge which these bulletins contain is worth to it all that the investigations have cost. But its greatest return lies in the fact that the company has added to its organization five men who have had this two-year period of advanced training. And each one of these five men is rapidly assuming a position of leadership in the company that more than justifies the special training that the cooperative arrangement has made possible.

My views may be summarized as follows:

The teaching of graduates and advanced undergraduates should be limited to men who have done and are continuing to do research work. This involves expenditures beyond the budgets of most universities. Industry profits by the research work of engineering professors, directly through the technical knowledge obtained, indirectly and to a greater extent, by the young men made available for employment whose advanced training has benefited by the research work of the professors. Cooperative investigations between industries and the universities in which the industries directly interested provide the funds with which to pay the direct expenses of the work and in which the universities provide the facilities and a professor to supervise the work, are profitable to industry and beneficial to the universities.

THE ENGINEERING FOUNDATION
WELDING RESEARCH COMMITTEE

Sponsored by American Welding Society and American Institute of Electrical Engineers

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Cooperative Research in Welding

By COMFORT A. ADAMS, *Chairman* WELDING RESEARCH COMMITTEE

RECENT advances in welding as a standard method of joining metal parts in manufacturing, construction, production and maintenance work have been almost phenomenal. As is true of arts of this sort, the practical applications tend to outstrip the applier's knowledge of the underlying sciences.

Failures and difficulties are, therefore, bound to occur in any new application or extension of the process. Today it is not unusual for industrial companies to spend several thousands of dollars, often $10,000 to $20,000, to overcome some particular difficulty. The same work may be duplicated to a considerable extent by competitors, or by others in allied industries. Moreover, such investigations are usually carried through only far enough to solve the immediate problem. Very often additional investigations of 20% or 25% the total cost could supply basic information for a thorough understanding of the phenomena involved. With this information at hand, kindred problems can be solved with little additional cost, and with far greater assurance as to correctness of final results.

It also happens that a great many preliminary investigations are extremely wasteful, and could be avoided if one had at hand in usable form a complete picture of existing knowledge on the subject which is available in the literature throughout the world.

The most rapid advances in welding have been made during the last decade. The frontiers of knowledge of the subject are being extended daily. The need for trained young men familiar with the fundamental principles, and having a thorough knowledge of the metallurgy involved is constantly growing.

Welding Research Committee

All of these facts led The Engineering Foundation to organize in 1935 the Welding Research Committee, sponsored jointly by the AMERICAN WELDING SOCIETY and the American Institute of Electrical Engineers for the purpose of (1) assembling, digesting and publishing available information on many specific subjects of broad interest in the welding field; (2) making provisions for the conduct of researches in our universities on specific fundamental problems needing solution, and thereby training young men in the fundamentals of welding; (3) stimulating and conducting needed research in the welding field; and (4) correlating existing and future programs of welding research. The activities of the Committee are carried on under three Divisions: Literature, Fundamental Research and Industrial Research. Progress reports on the work of each division will be found elsewhere in this bulletin.

Financial Support

Industry was quick to realize the importance of these objectives and cooperated with Engineering Foundation in launching this comprehensive program of research. The few industries approached have been liberal in their contributions. The present budget calls for an annual expenditure of $30,000 a year, with separate budgets and contributions for large investigations, such as, Fatigue Strength of Welded Joints, and the Effect of Carbon and Manganese on the Weldability of Steel.

Significance of the Work

Although operating on a reasonable scale for a period of about one year, the Committee has many important accomplishments to its credit.

Some contributors have stated that the critical digests of the literature alone are to them worth much more than the cost of their contribution. Others have found returns out of all proportion to the amounts which they have contributed, in the Fundamental Research Reports, or in securing a trained young man. The work of the Industrial Research Division, which has included in its membership the leading research experts in welding in the United States is now beginning to bear fruit.

Cooperation Needed

The Committee seeks the cooperation and goodwill of all who are interested in the advancement of welding. Funds are needed to carry out its important program of research work. Experts are needed to serve on its Divisions and give freely of their advice. The universities need help in obtaining specimens, and energetic guidance in the formulation of their problems. The cooperation of every one is earnestly invited to make this work effective.

Reports

Monthly reports summarizing results and progress made in each phase of the Committee's activities are rendered to contributors. Copies of these reports have been widely distributed to all cooperators, and to research workers in universities, industries and governmental departments.

Personnel

The present personnel of the Welding Research Committee includes:

C. A. Adams, *Chairman;* Consulting Engineer. Welding Research Committee, 29 West 39th Street, New York

Everett Chapman, President, Lukenweld, Inc., Coatesville, Pennsylvania

J. H. Critchett, Vice-President, Union Carbide & Carbon Research Laboratories, 30 East 42nd Street, New York

J. J. Crowe, Engineer-in-Charge of Apparatus Research and Development Department, Air Reduction Company, 100 Forrest Street, Jersey City, New Jersey

Arthur S. Douglass, Construction Engineer, Detroit Edison Co., Detroit, Michigan

C. L. Eksergian, Chief Engineer, Budd Wheel Company, 12141 Charlevoix Avenue, Detroit, Michigan

A. J. Ely, Mechanical Engineer, Standard Oil Development Company, Elizabeth, New Jersey

H. M. Hobart, Consulting Engineer, General Electric Company, Schenectady, New York

D. S. Jacobus, Advisory Engineer, The Babcock and Wilcox Company, 85 Liberty Street, New York

G. F. Jenks, Colonel, Ordnance Department, U. S. A., Army Munitions Building, Washington, D. C.

Jonathan Jones, Chief Engineer, Fabricated Steel Construction, Bethlehem Steel Company, Bethlehem, Pennsylvania

P. G. Lang, Jr., Engineer of Bridges, Baltimore & Ohio Railroad, Baltimore, Maryland

F. T. Llewellyn, Research Engineer, United States Steel Corporation, 71 Broadway, New York

Aubrey Weymouth, Vice-President and Chief Engineer, Post and McCord, Inc., 101 Park Avenue, New York (Alternate)

W. Spraragen, *Secretary;* Technical Secretary and Editor, AMERICAN WELDING SOCIETY, 29 West 39th Street, New York

Annual Report—Literature Division

By J. H. CRITCHETT, *Chairman*

THE need for assembling and digesting the information on welding concentrated in several hundred periodicals and published in many languages became apparent to The Engineering Foundation. This led to the formation of the Welding Research Committee with the understanding that one of the principal divisions would concern itself with critical digests of the literature.

In the early stages frequent meetings were held in order to develop a basic plan of operation. The first thought was the possibility of publishing several books. A comprehensive outline for such books was prepared.

Lack of funds and immediate needs for the critical digests of the literature on specific subjects by the Fundamental and Industrial Research Divisions led to a modification of these original plans.

Under the guidance of the Fundamental Research Division a number of critical digests were prepared on specific subjects which attracted world-wide attention.

The Welding Research Committee felt that, pending the formulation of a definite program for the publication of books, this work of critically digesting the literature on specific subjects should be continued under the auspices of the Literature Division.

The subjects selected for study are usually at the request of some member of the Fundamental Research Division, or some committee of the Industrial Research Division. A competent organization has been developed in the central office for making these critical digests, and for servicing experts to make such digests, where the services of experts can be secured. It is recognized by all that the ideal plan would be to have these digests made by the most competent experts. In most cases, however, they do not have the time, and in other instances they do not have facilities for securing foreign periodicals, or translations.

The function of a review of literature on any topic is to summarize all that has been published on that topic. The summary is first of all informative to any one not familiar with the topic. Second, the review serves as a guide to those familiar with the field and who wish to reach the source of any given fact or opinion concerning the topic. Third, the review makes it apparent what is not yet known concerning the subject under consideration. In these ways the review of the literature is valuable to the student, the expert and the research worker. The system used by the Literature Division parallels that employed by the Alloys of Iron Monographs. A detailed step-by-step procedure for making a review has been developed.

A record is made of all pertinent information collected at each source. Upon completion of the search the information is classified, all references dealing with a given sub-topic being grouped together for separate consideration. In many instances a single article will deal with two or more sub-topics.

The information under each sub-topic is then digested, particular attention being paid to exclude any trivial references but to include all quantitative determinations as well as all suggestive hypotheses. Whenever it is felt necessary, an appeal is made to outside experts for information on specific questions, or to a research investigator whose original work fails to describe essential experimental details, for the missing information. An effort is made to contrast conflicting data or opinions.

The final review consists of the sub-topics arranged in logical sequence, together with a bibliography, an appendix of general information on the topic under review (if necessary), a list of some of the research problems suggested by the review, and a summary. The summary conveys the salient established facts on each phase of the subject under review. The conclusions expressed in the summary are nearly always those arrived at by the investigators themselves. If there is conflict among reputable investigators, the matter is not included in the summary.

As soon as the review has been completed it is sent in mimeographed form to all who could conceivably offer comments, corrections or supplementary information. From 50 to 125 copies of each review are delivered with the request that discussion be sent to the reviewers within, usually, a month. Those who receive the mimeo-

graphed reviews are: 1st, members of the Welding Research Committee; 2nd, welding experts; 3rd, welding manufacturers; 4th, manufacturers of the product under review; 5th, large users of the product under review; 6th, authorities in the particular branches of science or engineering which are included, e.g., heat transfer, stress analysis and metallurgy.

Personnel

J. H. Critchett, *Chairman;* Union Carbide and Carbon Research Laboratories, Inc., 30 East 42 Street, New York
H. M. Hobart, General Electric Company, Schenectady New York
D. S. Jacobus, Babcock and Wilcox Company, 85 Liberty Street, New York, N. Y.
W. S. Spraragen, *Secretary*
C. A. Adams, Chairman Welding Research Committee; E. G. Budd Manufacturing Co., Philadelphia, Pa.

Translations

A unique cooperative plan for making available translations of important foreign articles relating to welding has been developed in cooperation with the Fundamental Research Division. A number of individuals and corporations have signified their willingness to make translations, or mimeograph reports, or both. In this way a very considerable number of translations have been made available. A list of the translations is published from time to time in the Research Supplement of THE WELDING JOURNAL.

Reports Published

A list of the critical digests of the literature prepared under the auspices of the Welding Research Committee follows:

Literature Division

1935—Bend Testing of Welds—A Summary, by M. F. Sayre, December.

1936—Impact Tests of Welded Joints, by W. Spraragen and G. E. Claussen, April.

1937—Fatigue Strength of Welded Joints—A Review of the Literature to October 1, 1936, by W. Spraragen and G. E. Claussen, January.

Welding of Copper and Its Alloys—A Review of the Literature to January 1, 1936, by Ira T. Hook, February and March.

Welding of Cast Iron—Review of Literature to Nov. 1, 1936, by W. Spraragen and G. E. Claussen, March.

Welding of Cast Steel—A Review of the Literature to November 1, 1936, by W. Spraragen and G. E. Claussen, April.

Stress Distribution in Fillet Welds—Review of the Literature to January 1, 1937, by A. G. Solakian and G. E. Claussen, May.

Welding Special Ferrous Alloy Castings—A Review of the Literature to November 1936, by W. Spraragen and G. E. Claussen, June.

Welding Malleable Cast Iron—A Review of the Literature to October 1, 1936, by W. Spraragen and G. E. Claussen, June.

Shrinkage Distortion in Welding, by W. Spraragen and G. E. Claussen, July.

Welding Aluminum and Its Alloys (German abstract), July.

Corrosion Resistance of Welded Joints—A Review of the Literature to January 1, 1937, by W. Spraragen and G. E. Claussen, August.

Temperature Distribution During Welding—A Review of the Literature to January 1, 1937, by W. Spraragen and G. E. Claussen, September.

Fusion Welding Wrought Iron—A Review of the Literature to January 1, 1937, by W. Spraragen and G. E. Claussen, September.

The Effect of Total Carbon and Manganese on the Mechanical Properties and Structure of Welded Joints in Plain Low-Carbon Steel—A Review of the Literature to January 1, 1937, by W. Spraragen and G. E. Claussen, September.

The Heat Effect in Welding—A Review of the Literature to January 1937, by Walter H. Bruckner, October.

Shrinkage Stresses in Welding—A Review of the Literature to January 1, 1937, by W. Spraragen and G. E. Claussen, November.

Annual Report Fundamental Research Division

By H. M. HOBART, *Chairman*

General

THE Fundamental Research Division is one of the three divisions affiliated with the Welding Research Committee. In age it antedates its parent organization, having been affiliated with the American Bureau of Welding for a number of years, and having been transferred to the Welding Research Committee about two years ago.

There are two main purposes of the Fundamental Research Division. These are: (1) to interest the colleges of this country in problems which relate to welding in order to familiarize the professors and students of these universities with this growing and important tool; and (2) to bring to bear upon fundamental problems needing solution the scientific minds which are free from commercialism and from the necessity of producing results of immediate commercial value. In other words, the University atmosphere should be most conducive to pursuing research until the truth is disclosed as compared with discontinuing it as soon as just enough data have been obtained to serve the immediate commercial objective. As Professor Adams has stated many times, "If we could only learn the fundamental truths underlying a certain phenomenon, the reason why of things, we could with very little additional investigation apply this knowledge to the solution of dozens of practical problems, and with greater degree of certainty as to the correctness of these solutions." Both these purposes have been successfully accomplished. We now have more than 40 professors actively engaged in various

problems, and the reports which result from their research work each year form the basis for a considerable number of important papers which are usually presented at the annual meeting of the AMERICAN WELDING SOCIETY.

Methods of Operation

Probably no other organization in the engineering world operates with less organization and with greater opportunism. The underlying motif has been to interest each professor in some problem, and to assist in every way possible in the furtherance of such research work as he may undertake. The officers have been very careful not to unduly urge upon any professor any particular problem.

As indicated elsewhere in this report, a list of reseach problems needing solution, which is revised from time to time, is offered for study and to provoke interest among the various universities.

When a professor (or other worker in fundamental research) undertakes a specific problem, everything possible is done to assist in the formulation of the problem, in helping the professor make contacts with experts in industry, in tying up with local companies in order to secure specimens, and in a very few instances small grants-in-aid have been made or assistance of this sort rendered in other ways.

For the past four or five years a conference has been held annually, usually at the time of the annual meeting of the AMERICAN WELDING SOCIETY, and the railroad and pullman expenses of attending professors have been defrayed. Lately because of limitation of funds, it has been necessary to limit the amount appropriated to any one individual to $50. At this conference the research workers meet with one another and compare their research experiences, and, through correspondence, are helpful to each other after returning to their respective laboratories. The conference also enables them to make acquaintances with many people in the welding industry.

In order to assist still further in mutual cooperation, local groups have been organized in several industrial centers with the idea that such groups will serve as an inspiration to other universities in the vicinity, and supply the needs of the professors in the particular locality.

It is not at all surprising that these groups have operated with varied success, most of them in entirely different ways; and the good accomplished has in nearly every instance been of an opportunistic nature.

Research Conferences

This year a highly successful conference of professors and others interested in fundamental research was held in Atlantic City on October 19th, during the annual meeting of the AMERICAN WELDING SOCIETY. More than 100 members of the Division and of the Society were present.

This conference was given over to inspiring addresses by representatives of the Fundamental Research Division, interested committees of the American Institute of Electrical Engineers, representatives of industry, and reports by the Chairman of the Welding Research Committee and officers of the Fundamental Research Division. These addresses were followed in each instance by discussion from the floor.

It is impossible to portray in words the value and benefits which accrue to those attending such conferences. For example, it was necessary to be present to obtain the inspirational value of such a talk as Professor Wilson gave on the value of research to the university, to the professor, and to the student, and of the address by Professor Adams on the importance of fundamental research as compared with applied investigations. Other phases of this conference will be brought out under the respective headings which follow.

List of Research Problems

A List of Fundamental Research Problems in Welding was published in November 1936. A new list is being compiled, and will be published shortly. This pamphlet will contain:

(1) A statement of the aims of the Division;
(2) A list of research reports, reviews of literature and translations;
(3) Lists of important current research problems needing solution, with some attempt at their formulation;
(4) References to recent literature on a few selected topics;
(5) First steps in Welding Research. A brief article presenting some helpful suggestions as to making use of facilities available at the universities and the central office;
(6) A list of the investigations now under way in the various universities, and the professors in charge.

Periodic Visits to Universities

It seems to your Chairman that there ought to be at all times closer cooperation between the central office and the various universities. During the past year visits have been made by the Chairman, Secretary and Dr. Claussen, to a number of universities including the following:

University of Pittsburgh
Ohio State University
Case School of Applied Science
Union College
Rensselaer Polytechnic Institute
Johns Hopkins University
Harvard University
Massachusetts Institute of Technology
New York University
Columbia University
Webb Institute of Naval Architecture
University of Pennsylvania
Drexel Institute of Technology

Inasmuch as one of the purposes of these visits is to encourage cooperation locally between these universities and industrial research laboratories in the country, and experts in industry, these visits have also included other laboratories and plants, such as:

U. S. Bureau of Standards
U. S. Department of the Navy
U. S. Steel Corporation
Watertown Arsenal
Wellman Engineering Company
General Electric Company
Westinghouse Electric & Manufacturing Company

Professors are encouraged to call upon the Chairman, the Secretary or Dr. Claussen to visit their universities and consult with the particular professors, members of the departments or local companies to assist in expediting possible research work.

In one instance such a visit with one professor resulted in a consulation with the head of a department, and finally with the president of the university, which led to the purchase of some $15,000 worth of equipment, and the establishment of a research fellowship at the university. This equipment will enable the university to become an educational and research center for welding.

Library Research

One of the pet hobbies of your Chairman has been what he terms "Library Research." Briefly, he is of the opinion than in many instances researches made by delving into the literature, and by correlating the results of the information contained therein, can reveal important conclusions which will be of equal importance to laboratory research work, and in many instances may avoid the necessity for any further laboratory research work. In any event, such library researches should precede laboratory research work. They should form the basis for any new work undertaken.

In practice it has been found difficult to carry out this theory for several reasons, the most important of which are:

(1) Most libraries do not have a complete list of the most important periodicals devoted to welding;

(2) Most library research men in this country are unable to read any language other than English.

With a view to overcoming these difficulties, there have been established in the central office facilities for making available photostats, abstracts and translations of important periodicals. We believe we can safely say that if any professor is vitally interested in making a critical digest of the literature on any selected subject, facilities will be provided him along the lines above.

In order to serve as an example of what can be done in this field, the central office undertook to prepare critical digests of the literature on selected subjects, a number of which have been completed and furnished to all those interested. Mention has been made that the Welding Research Committee has two other divisions, namely, Literature and Industrial. In order to appropriately divide the work, the preparation of these critical digests was turned over to and made part of the Literature Division on April 23, 1937. However, professors are encouraged to undertake critical digests on those subjects in which they are interested, and we will see to it that they are furnished with the needed facilities to enable them to make the digests, which, when completed, will be turned over to the Literature Division.

Through a cooperative arrangement translations of important articles have been made. This cooperative arrangement involves quite a list of persons, some who are willing to make translations, others who are prepared to do mimeographing or duplicating work and still others who will do both. In this way about thirty complete translations, or extended abstracts, have been made during the past year, which are available either through loan, or direct gift to all interested. A list of translations is published from time to time in the Research Supplement which appears monthly. This cooperative plan of making translations available was also turned over to the Literature Division. Here again, however, university workers are urged to cooperate in making the translations, in offering mimeographing facilities or in making use of the translations when made available.

Published Reports

Aside from the critical digest of the literature mentioned above, this Division has to its credit a number of published reports during the year, as follows:

Welded Beam-Column Connections—I. Lyse and G. J. Gibson

The Effect of Low Temperature on the Tensile Impact Resistance of Iron, Steel and Welded Joints—O. H. Henry

Welded Structural Brackets—C. D. Jensen

Electric Heating by the Proximity Effect—Edward Bennett

Characteristics of a Universal Welding Generator—N. F. Ward

A List of Fundamental Research Problems

Micro Testing Machine, Chevenard, tr. by M. Scioletich

Progress in Welding During Last Six Months of 1936

Strain Measurements in Welded Joints—J. L. Banville and H. A. Storrs

An Investigation of Plug and Slot Welds—G. J. Gibson

Peening and Its Effects on Arc Welds—E. M. MacCutcheon, Jr., and D. M. Kingsley, Jr.

Welded Girders with Inclined Stiffeners—C. D. Jensen and W. F. Lotz

Welding of Cooper—A. P. Young

Static and Impact Tensile Properties of Stainless Steel Welds at Ordinary and Low Temperatures—O. H. Henry

Effect of Welded Top Angles on Beam-Column Connections—Inge Lyse and G. J. Gibson

An Alternating Current Non-Destructive Test for Welded Seams—W. B. Kouwenhoven and A. E. Vivell

X-ray Methods of Studying Stress Relief in Welds—J. T. Norton

Fatigue Tests of Butt Welds in Structural Plates—W. M. Wilson

Studies of the Oxyacetylene Cutting Process—H. R. Bullock

Activities at the Universities

Besides requesting things, the professors are now making fine headway in their fundamental researches. For example, Professor Hess, of R. P. I, is completing the installation of resistance welding equipment in his laboratory with expectation of embarking at once on an extensive research program. Professor Wilson, University of Illinois, will continue his fatigue studies on full-scale welded joints as soon as the financing is completed. At Johns Hopkins University, Professor Kouwenhoven is using his laminated specimens to test the sensitivity of the magnaflux method of non-destructive testing. Also continuing their researches along lines familiar to them are Professor Young at Michigan College of Mining & Technology on Copper Welding, and Professor Henry, of Polytechnic Institute of Brooklyn, on low temperature static and impact tensile tests. Creep tests of arc welds and heat transfer in resistance welding are the topics on which Professor Ward is working. Structural welding research at Lehigh is being continued by Professor Jensen, who is going to find out all there is to know about the stiffening of plate girders with welded inclined stiffeners. He is also studying the stress distribution in fillet welds subjected to flexure. The research from the Fritz Engineering Laboratory under Professor Lyse has provided much of the quantitative information we now have on structural welding. The estimation of porosity in welds by improved radiographic technique is Professor Norton's research problem at M. I. T. Professor Graf, of Oregon State Agricultural College, has conducted some of the research work on welding at the Bonneville Dam and expects that his associates at the Engineering Experiment Station will tackle some of the fundamental problems during the coming year. Mr. Jennings of Westinghouse, who is abroad just at present, has studied the welding of austenitic stainless manganese-nickel steel. I am glad to report that Professor Jamieson, of McGill, one of the exceedingly few people on this side of the Atlantic to do anything definite about determining shrinkage stresses, is going to resume his experiments this year. Professor Hollister of Cornell, of course, is hard at work on fatigue tests of large fillet welded specimens using machines of

FUNDAMENTAL RESEARCH DIVISION

Alphabetical List of Researchers, Their Affiliations and Their Subjects. (As Revised October 1937)

	Research Being Conducted by	Subject of Research
1.	Prof. Edward Bennett Univ. of Wisconsin	Studies Relating to the Selective Heating of Large Conducting Bodies in Strips by Utilizing the "Proximity Effect" to Control the Distribution of Heating Currents of the Audio Frequency Range
2.	Prof. H. Beresford Univ. of Idaho, Agricultural Engg. Dept.	Application of Wear Resistant Surfaces to Agricultural Equipment and Tools
3.	Prof. H. M. Boylston Case School of Applied Science, Dept. of Metallurgy	(*a*) Metallurgical Investigations on Welds (*b*) Factors Influencing the Welding Quality of Steel
4.	H. R. Bullock Massachusetts Institute of Technology	Study of Oxyacetylene Cutting
5.	Prof. H. L. Daasch Iowa State College, Dept. of Mech. Engg.	Flame Cutting
6.	Prof. R. P. Davis West Virginia Univ.	Inactive at Present
7.	Prof. A. V. deForest Massachusetts Institute of Technology, Mech. Engg. Dept.	Vibration Studies of Welded Structures
8.	Prof. D. J. Demorest Ohio State Univ.	Coatings for Welding Rods
9.	Prof. J. P. DenHartog Harvard Univ.	Influence of the Welding Heat on the Structure of the Adjacent Metal
10.	Prof. G. E. Doan Lehigh Univ., Dept of Physical Metallurgy	(*a*) Causes and Conditions of Crater Formation in Arc Welding (*b*) A Study of the Basic Nature of the Arc Discharge Under Conditions of High Purity (*c*) The Properties of Weld Metal Made with Pure Iron in Argon and Other Atmospheres (*d*) The Use of Gamma Rays for Weld Examination (*e*) Acceleration of Rate of Welding by Additions to the Welding Wire
11.	Dr. F. C. Fair New York Univ.. Dept. of Chemical Engg.	Metallography of Alloy Steel Welds
12.	Prof. S. L. Goodale Univ. of Pittsburgh, Dept. of Metallurgy	(*a*) Annealing of Welded Joints (*b*) Strain Annealing of Medium Carbon Steel (0.25 to 0.30%)
13.	Prof. S. H. Graf Oregon State Agricultural College	(*a*) X-Ray Diffraction Studies on Stresses in Welds (*b*) Heat Effects in Weld Metal
14.	Prof. P. R. Hall Pennsylvania State College, Dept. of Industrial Engg.	Welding of Cast Iron
15.	Prof. O. H. Henry Polytechnic Institute of Brooklyn, Dept. of Mech. Engg.	Effect of Low Temperature on the Static and Dynamic Tensile Strength of Welded Joints in Some Ferrous and Nonferrous Metals; Fatigue Tests of Welded Joints at Low Temperatures
16.	Prof. W. F. Hess Rensselaer Polytechnic Institute, Dept. of Metallurgy	(*a*) Ignition of Alternating Current Arcs (*b*) Impact Studies of Fillet Welds (*c*) Resistance Welding
17.	Prof. N. S. Hibshman Lehigh Univ.	Inactive at present
18.	Prof. S. C. Hollister Cornell Univ.	Locked-Up Stresses in Welds; Repeated Stress in Welded Connections
19.	Prof. T. P. Hughes Univ. of Minnesota, Dept. of Mech. Engg.	Relation of Current and Voltage to Speed of Welding Ductility Values as Obtained by the Tensile and Cold-Bend Tests
20.	Prof. M. A. Hunter Rensselaer Polytechnic Institute	
21.	Prof. R. E. Jamieson McGill Univ. Montreal, Dept. of Civil Engg.	Initial Stress Caused by Welding
22.	C. H. Jennings Westinghouse E. & M. Co., Pittsburgh	Strength and Design of Welded Joints
23.	C. D. Jensen Lehigh Univ., Civil Engg. Dept.	(*a*) Welded Girders with Inclined Stiffeners (*b*) Stress Distribution in Fillet Welds Subjected to Bending
24.	W. C. Johnson Princeton Univ., Dept. of Electrical Engineering	Spot Welding
25.	Prof. W. B. Kouwenhoven Johns Hopkins Univ.	(*a*) Flux Penetration Tests (*b*) Non-Destructive Tests for Welds
26.	Prof. W. J. Krefeld Columbia Univ., Dept. of Civil Engg.	Welded Beam Connections

27.	Prof. T. R. Lawson Rensselaer Polytechnic Institute, Head, Dept. Civil Engg.	(*a*) Quantitative Studies of the Stresses Caused by Welding Operations (*b*) Photoelastic Studies of Stresses in Riveted and in Welded Joints (*c*) Impact Stresses in Welded Joints
28.	Dr. H. H. Lester Watertown Arsenal	X-Ray Specifications for Castings and Welded Structures
29.	Prof. Inge Lyse Lehigh Univ., Fritz Engg. Lab.	(*a*) Welded Reinforcing Bars (*b*) Rigidity of Welded Structural Connection (*c*) All-Welded Vierendeel Trusses
30.	Prof. Joseph Marin Rutgers Univ., Dept. of General Engg.	Poly-Axial Stresses
31.	Dr. D. W. Murphy Univ. Michigan, Dept. of Engg. Research	Determination of Oxygen and Nitrogen Content
32.	Dean J. A. Needy Ohio Northern Univ., College of Engg.	Problems in the Welding of Cast Iron
33.	Prof. J. T. Norton Massachusetts Institute of Technology, Dept. of Mining & Metallurgy	(*a*) Radiographic Technique of Examining Welds (*b*) X-Ray Diffraction Method for Investigating Stresses in Welds (*c*) X-Ray Studies of Age-Hardening
34.	F. A. Rappolt College of the City of New York	Welded Beam Connections
35.	Prof. O. D. Rickly Ohio State Univ., Dept. of Industrial Engg.	Study of Residual Stresses in Welded Joints, Shear and Impact Tests of Welds
36.	Prof. M. F. Sayre Union College, Dept. of Civil Engg.	Development of Methods of Testing Welds
37.	Prof. G. S. Schaller Univ. Washington, Dept. of Mech. Engg.	(*a*) Development of an Arc with a Gaseous Atmosphere (*b*) Oxyacetylene Welding of Cast Iron
38.	Prof. R. R. Seeber and A. P. Young Michigan College of Mining and Technology, Dept. of Mech. Engg.	(*a*) Copper Welding (*b*) Method of Testing Welds
39.	Prof. A. H. Sluss and V. M. Smith Univ. of Kansas	Fatigue Testing of Fusion Welded Butt Joints
40.	A. Solakian Columbia Univ.	Photoelastic Studies
41.	Prof. W. A. Spindler Univ. of Michigan, Dept. of Metal Processing	Welding Cast Iron
42.	Prof. J. G. Tarboux Univ. of Tennessee	Magnetic Field Intensities Surrounding Arcs
43.	Dr. E. Therkelsen and G. J. Pesman Montana State College, Dept. of Mech. Engg.	Distortion of Welded Pipe Structures
44.	Prof. G. E. Thornton State College of Washington, Dept. of Mech. Engg.	Fatigue Tests
45.	Dr. A. E. Vivell Princeton Univ., Dept. of Electrical Engg.	Non-Destructive Testing
46.	Prof. N. F. Ward Univ. California, Dept. of Mech. Engg.	(*a*) Creep Tests of Arc Welds in Steel (*b*) Heat Transfer in Resistance Welding
47.	W. L. Warner Watertown Arsenal	(*a*) Heat Effect of Arc Welding on Various Alloy Steels (*b*) Effect of Preheating on Weldability of These Steels
48.	Prof. A. E. White Univ. of Michigan	(*a*) Creep of Metals and Welded Structures at High Temperatures (*b*) Resistance to Oxidation, Embrittlement and Fatigue (*c*) Stresses in Annealed and Unannealed Welded Pipe
49.	Prof. W. M. Wilson Univ. of Illinois, Dept. of Civil Engg.	Fatigue Tests of Large Welded Specimens
50.	S. F. Yasines New York Univ., Dept of Civil Engg.	Notch Impact Tests of Welds in Low-Alloy Steel
51.	Dr. M. Yatsevitch Watertown Arsenal	Coefficient of Expansion of Weld Metal

his own design. Professor Thornton, of Washington State, is also planning fatigue tests using a rotating bend machine. A timely subject from the standpoint of the country's largest mineral industry is Professor Therkelsen's study of the distortion of welded oil refinery piping. Dr. Therkelsen is professor at Montana State. At Ohio State, Professor Demorest is getting at what constitutes a controlled welding atmosphere. One of his students is working on the welding of nickel alloys. Finally, Professor Daasch, of Iowa State, is tackling the thermodynamic problems of flame cutting, and Professor Yasines, of N. Y. U., has under way some notch impact tests of welded low-alloy steels.

Local Committees

The philosophy of operation of these local groups has been explained above. It seems, however, that a concerted effort should be made on the part of the central

office and the individual professors to cooperate locally either with a company or laboratory, or groups of companies, *or with the local sections of the* AMERICAN WELDING SOCIETY in order that the needs of each professor may be promptly taken care of without the routine of going through the central office. The more these operations become decentralized, the greater will be the progress. The central office feature of our plan of organization is well adapted to fitting varied needs and preferences. Any group can be autonomous to whatever extent it prefers. When it believes that the centeral office can be of assistance, it will apply. The central office has a fine permanent staff ready to assist in all ways possible.

Cooperation with Other Divisions

Mention has been made of the cooperation with the Literature Division of the Welding Research Committee. There is another division, The Industrial Research Division, of which Colonel G. F. Jenks is Chairman. This Division has a number of Material and Functional Subcommittees, and I am sure each of these committees will be glad to cooperate with any professors interested in the respective fields in which these subcommittees operate.

Work Now Underway

A revised list of university research workers and their problems supplements this report. It is unfortunate that a report of this nature cannot go into greater detail than a summary statement of the excellent work being done by some of the professors, and, on the other side, the very limited work being done in other localities.

Conclusion

A smooth running and very flexible mechanism has been provided for mutual cooperation and the stimulation of fundamental research work in the universities of this country. Considerable progress has been made, of which all those who participated can be justly proud. Opportunities for service are almost limitless.

I do not wish to go on record as stating that the central organization is prepared to meet every demand which can be made upon it in the way of assistance, but we can safely state that if any professor indicates a genuine interest in some fundamental research problems in the welding field, every assistance possible will be rendered.

Annual Report—Industrial Research Division

By COLONEL G. F. JENKS, *Chairman*

Introduction

IN organizing the Industrial Research Division of the Engineering Foundation—Welding Research Committee, the principal field of activity and usefulness has been considered to fall into three groups.

1. The organization and coordination of research activities in the field of welding required by technical organizations in the performance of their functions. Contacts between this Division and several committees and subcommittees of the American Society for Testing Materials have been established by which this Division will initiate and coordinate research programs to furnish technical information essential to the preparation of specifications. Similar contacts with other technical bodies are being developed. To fill these functions the Division has been organized in Material committees, which corresponds in general with the organization of other technical bodies.

2. The organization and coordination of welding research activities of general interest to the industry. For a full understanding and intelligent application of welding, much remains to be accomplished in studying the fundamentals of welding. There are programs of fundamental research in many laboratories. Considering the magnitude of the problems, there is a limited personnel available. It is, therefore, important that duplication of effort be avoided. In its committee organization, those engaged in limited fields of activities are being brought together. In this way it is expected that much duplication of effort will be avoided and that each laboratory will approach a problem with a better knowledge of work accomplished elsewhere, and with a better grasp of the problem which arises from round table discussions. This organization also provides the much needed contact between the industrial and university laboratory. In general, the Functional committees have been organized to meet these requirements.

3. The organization and fostering of research activities in the field of inspection and testing of materials used in welding and of welded joints and structures. These activities are being carried on in close cooperation with other technical organizations, especially the American Society for Testing Materials. Advantage is being taken of the facilities and personnel of educational institutions in studying these problems.

In an organization of this type, self-coordination is the best form of coordination. Self-coordination can be established only if those responsible for activities are regularly brought together and recognize the economy of effort from cooperation. In organizing committees, those directly responsible and most active in the various fields have been selected. Some of the most important accomplishments come from the coordination of research efforts arising from the discussions of these groups which will never appear in the reports of the Division.

A beginning has been made in perfecting contacts between industry and the laboratories of educational institutions. Two grants have been made for research work at educational institutions under the control of committees of the Division. Other work without direct grants is being followed. Some important projects of this nature are being organized. This cooperative effort furnishes the university with needed industrial contacts and gives industry the much needed fundamental view-point. A purpose of this Division is to furnish the organization through which such cooperative effort develops naturally. Further developments along these lines are most promising.

In building up these contacts with educational institutions advantage is being taken of the experience and cooperation of the Fundamental Research Division under the chairmanship of Mr. H. M. Hobart. The accomplishments of that Division are outstanding. It is ad-

vantageous both to industry and to educational institutions to perfect the organization by which representatives of each sit down and discuss both the requirements of and also the scope and possibilities of fundamental research for which our universities are so well adapted.

In the preliminary analysis of research problems, this Division has been assisted by the Literature Division for library research work, both in general fields and for special problems. Much of the library work requested by this Division has been made available by publication in THE WELDING JOURNAL. This library work has been of great assistance to the committees of this Division.

There is in various laboratories a large volume of unpublished reports of welding research work. Some of this has been made available to the Division. A request for such reports in a special field will be made shortly. It is hoped that all laboratories having unpublished reports will make them available to the Division for analysis and study. In this manner any research projects organized will take advantage of prior work and be conducted with a fuller understanding of principles involved. The full cooperation of all laboratories is expected.

It is to be noted that the interests of this Division are general and fundamental—problems normally beyond the scope of a single organization, which tax the utmost human efforts, and which can be solved only through the assistance and cooperation of industry.

Division of Work

Corresponding with the organization of industry, a number of Material committees have been appointed as follows:

I—Cast Iron (not yet organized)
II—Carbon Steels
III—Low-Alloy Steels
IV—High-Alloy Steels
V—Aluminum Alloys
VI—Copper Alloys
VII—Nickel Alloys

These vertical divisions have been supplemented by the appointment of a number of Functional committees which must, of necessity, cut across and supplement work of the Material committees. So far the following Functional committees have been appointed:

A—Methods of Testing
B—Analysis of Weld Failures (not yet organized)
C—Weld Stresses—Causes and Effects
D—Non-Destructive Tests
E—Resistance Welding (not yet organized)
F—Fatigue Testing (Structural)

Others will be added as necessity requires.

General Meetings

A number of general meetings of the Industrial Research Division have been held as follows:

1936

February 16th—New York, N. Y.
July 23rd–24th—Watertown Arsenal, Watertown, Mass.
October 21st—Hotel Cleveland, Cleveland, Ohio

1937

June 29th—New York, N. Y.
October 21st—Atlantic City, N. J.

Reports Published

Arc Welding of Structural Alloy Steels, by W. L. Warner, October 1936.

The Nickel-Copper High-Strength Steels for Welded Construction, by G. E. Gibson, February 1937.

Welding High-Tensile Boiler Steels, by H. Aysslinger, June 1937.

Electric Arc Welding of Monel Metal Structures, by J. F. Maguire, July 1937.

Loss of Material in Flash Welding and Automatic Flash Welders, by E. Rietsch, July 1937.

Spot Welding Characteristics of Some Copper-Base Alloys, by D. K. Crampton and J. J. Vreeland, October 1937.

Committee Activities

Plain Carbon Steel—J. C. Hodge, Chairman

This Committee has studied requests of the American Society for Testing Materials for data concerning weldability of steel included in A. S. T. M. Specification A-151-35, which was recently withdrawn, and has also studied past research work, especially on the influence of carbon and manganese on the weldability of plain carbon steels.

The Committee has drafted a research program covering a comprehensive investigation of the effect of carbon and manganese on the welding properties of steel. The program includes 4 ranges of carbon, 5 ranges of manganese and 3 thicknesses of plate. Wide publicity will be given to this program for criticism and it will then be reconsidered, and, it is hoped, put into execution in the near future. Steps are also being taken to secure research data and experience concerning the behavior of various elements of plain carbon steel.

Low-Alloy Steels—J. H. Critchett, Chairman

This Committee has compiled and correlated information on the weldability of various low-alloy steels. These steels have been classified into three groups as follows: (1) foolproof from the welding standpoint; (2) steels which are moderately air-hardening under average welding conditions; (3) steels in which the zone next to the weld undergoes sufficient loss of ductility so that it becomes serious from the engineering standpoint. This report will be published in the January issue of the Research Supplement. Wide criticism of this report is invited.

High-Alloy Steels—T. H. Nelson, Chairman

This Committee is operating in close cooperation with Committee A-10 of the A. S. T. M. In its current program attention will be given primarily to the general subject of the heat treatment of welded high-alloy steel equipment. Contact is being developed with the Boiler Code Committee to ascertain the requirements of that Committee in respect to research activities.

Aluminum Alloys—G. O. Hoglund, Chairman

This Committee will concern itself with the establishment of basic data and information which industry requires for the development of welding technique codes. Research will be conducted to determine the effect of welding heat on the aluminum materials containing alloying constituents, particularly the heat treatable alloys. Techniques and filler metal will also be carefully studied, as will be special problems in connection with the application of the various welding processes. All welding processes will be considered.

Copper Alloys—D. K. Crampton, Chairman

This Committee intends to concentrate its initial work on the fundamental problems relating to the resistance welding of copper. A critical digest of the literature will be made which will be supplemented by information secured from interested companies. The program of research will include investigation of energy applied, rates of dissipation of heat, measurement of surface contact resistance, and a study of the causes of various types of unsoundness in welds. Methods of test for the rating of spot welds will also be developed, and investigations conducted on studies of instantaneous temperatures attained at various points in resistance welding, and the correlation of this information with other variables. A research program is being organized at the Rensselaer Polytechnic Institute under the advisory supervision of this Committee.

Nickel Alloys—O. B. J. Fraser, Chairman

This Committee contemplates a survey of the literature on nickel alloys containing 40% or more of nickel, and to pure nickel. The digest of the literature will be supplemented by information secured through the aid of a questionnaire. A special report is under preparation covering research dealing with the physical characteristics and corrosion resistance of welded joints in nickel clad steels. This Committee submitted, at the recent Annual Meeting of the AMERICAN WELDING SOCIETY, a review of work recently accomplished in the welding of nickel alloys.

Methods of Testing—M. F. Sayre, Chairman

This Committee held with the A. S. T. M. a joint round table conference in New York in June, during the A. S. T. M. Convention, at which were present some 300 persons interested in the subject of Impact Testing. It is preparing a program for a symposium on Impact Testing which it is hoped will be held during the coming year. The proposed program includes papers dealing with theory, importance of velocity of loading, temperature, effect of notch concentration, correlation of impact tests with field experience, and impact tests on welded specimens.

Weld Stresses—Causes and Effects—Everett Chapman, Chairman

A preliminary report dividing the problem into two components, namely:

1. Brittle failures that have been noted in steels otherwise ductile under biaxial tension loading.
2. Residual stresses in large areas caused by temperature gradients of biaxial nature. The problem will be attacked from four angles including library research, survey of industrial case histories, development of methods of measurements and pertinent experiments.

Work on the weld stress problem is underway at the Harvard University in a program in which Watertown Arsenal is cooperating, and at Union College. It is hoped to initiate programs at other universities in the near future.

Non-Destructive Tests—J. T. Norton, Chairman

The first subject selected for consideration by this Committee is the correlation of radiographic tests and mechanical tests of welded joints. A questionnaire submitted to a considerable number of those in the welding industry has revealed the fact that such information would be desirable, but has plainly shown that there is very little material available at present upon which such a correlation could be based. Accordingly, a tentative research program has been set up for consideration by the Committee.

One phase, namely, the examination of a method of classifying the radiographs of welds in a quantitative fashion has been started. This investigation will consist of a suitable recording densitometer, determination of the density thickness relationship for various experimental conditions, application of the method to typical welds, and attempts to correlate results with mechanical properties. This work has been organized at the Massachusetts Institute of Technology.

Resistance Welding

This Committee has not yet been organized, but prospects of developing a comprehensive program to deal with the fundamentals of resistance welding are very good.

Fatigue Testing (Structural)—Jonathan Jones, Chairman

The need for data relating to fatigue properties of large-size welded joints has been stressed by the Bridge Committee of the AMERICAN WELDING SOCIETY. The present bridge specifications are based largely on experiments carried out abroad. The need for information relating to American conditions is apparent to all those interested.

Some work has been started by the Structural Steel Research Committee at Cornell University, and some preliminary experiments on large-size joints have been made by Professor Wilson, of the University of Illinois.

The Committee is now being rapidly organized and a program is being prepared for research activities on the fatigue strength of welded structures. The program contemplates extensive work at the University of Illinois.

Personnel—Industrial Research Division

There follows a list of the personnel of the Industrial Research Division and its committees:

Membership

*Jenks, Colonel, G. F., *Chairman*, Chief of Technical Staff, Office of the Chief of Ordnance, War Department, Washington
*Adams, C. A., Edward G. Budd Manufacturing Company
Bibber, L. C., Welding Engineer, Carnegie-Illinois Steel Corporation
Boardman, H. C., Director of Research, Chicago Bridge and Iron Works
*Chapman, Everett, President, Lukenweld, Incorporated
*Crampton, D. K., Research Director, Chase Brass and Copper Company, Incorporated
*Critchett, J. H., Vice-President, Union Carbide and Carbon Research Laboratories
*Crowe, J. J., Engineer-in-Charge of Apparatus, Research and Development Department, Air Reduction Company
Deppeler, J. H., Chief Engineer and Works Manager, Metal Thermit Corporation
Eksergian, C. L., Chief Engineer, Budd Wheel Company
*Fraser, O. B. J., Superintendent, Technical Service, The International Nickel Company, Incorporated
French, H. J., In Charge, Alloy Steel and Iron Development, Development and Research Department, The International Nickel Company, Incorporated

* Executive Committee Member.

Gibson, A. E., President, The Wellman Engineering Company
Halsey, W. D., Assistant Chief Engineer, Boiler Division, The Hartford Steam Boiler Inspection and Insurance Co.
*Hobart, H. M., Chairman, Fundamental Research Committee; Consulting Engineer, General Electric Company
*Hodge, J. C., Chief Metallurgist, The Babcock and Wilcox Company
*Hoglund, G. O., Welding Engineer, Aluminum Company of America
Jasper, T. M., Director of Research, A. O. Smith Corporation
Jennings, C. H., Engineer in Charge of Welding Research, Westinghouse Electric and Manufacturing Company
Jennison, H. C., Technical Manager, The American Brass Company
Johnson, J. B., Chief, Material Branch, Wright Field
*Jones, Jonathan, Chief Engineer, Bethlehem Steel Company
Kidd, A., Assistant Works Manager, The M. W. Kellogg Company
Lang, P. G., Jr., Engineer of Bridges, Baltimore and Ohio Railroad Company
Leveen, L. R., Engineer, General Electric Company, Schenectady
Lincoln, J. F., President, The Lincoln Electric Company
*Llewellyn, F. T., Research Engineer, United States Steel Corporation.
Loos, C. E., American Bridge Company
Madsen, H. V. B., Lieutenant, Bureau of Construction and Repair, Navy Department
Miller, H. L., Metallurgical Department, Republic Steel Corporation.
*Mochel, N. L., Metallurgical Engineer, Westinghouse Electric and Manufacturing Company
Moore, G. H., Jr., Welding Engineer, Welding Office, Production Department, Newport News Shipbuilding and Drydock Company
*Nelson, T. H., Consulting Metallurgist, The Midvale Steel Company
*Norton, J. T., Associate Professor of Physical Metallurgy, Massachusetts Institute of Technology
Officer-in-Charge, Specifications Section, Design Division, Bureau of Engineering, Navy Department
*Sayre, M. F., Professor, Department of Applied Mechanics, Union College
Schenck, Charles, Engineer of Development, Bethlehem Steel Company
*Spraragen, W., Secretary, Welding Research Committee
Underwood, C. M., Senior Welding Engineer, U. S. Naval Gun Factory
Wallace, L. W., Director, American Railway Research Advisory Committee
Weigel, A. C., Vice-President, Combustion Engineering Corporation

Material Committees

I. Cast Iron—(Chairman not yet named)

II. Carbon Steels—J. C. Hodge, *Chairman;* The Babcock and Wilcox Company

Members:

R. H. Aborn, United States Steel Corporation
A. B. Bagsar, Sun Oil Company
E. C. Chapman, Hedges-Walsh-Weidner Company
J. H. Critchett, Union Carbide and Carbon Research Laboratories
J. H. Deppeler, Metal and Thermit Corporation
A. S. Douglass, The Detroit Edison Company
Paul Ffield, Bethlehem Shipbuilding Corporation
F. C. Fyke, Standard Oil Development Corporation
H. V. B. Madsen, Bureau of Construction and Repair, Navy Department
N. L. Mochel, Westinghouse Electric & Manufacturing Company
Navy Department, Officer-in-Charge, Specifications Section, Design Division, Bureau of Engineering
Charles Schenck, Bethlehem Steel Company
W. G. Theisinger, Lukens Steel Company

Subcommittee II

Effect of Carbon and Manganese on the Weldability of Steel—W. G. Theisinger, *Chairman;* Lukens Steel Company

Members:

A. B. Bagsar, Sun Oil Company
Navy Department, Officer-in-Charge, Specifications Section, Design Division, Bureau of Engineering

III. Low-Alloy Steels—J. H. Critchett, *Chairman;* Union Carbide and Carbon Research Laboratories

Members:

L. C. Bibber, Carnegie-Illinois Steel Corporation
Everett Chapman, Lukenweld, Incorporated
W. E. Crawford, A. O. Smith Corporation
J. H. Deppeler, Metal and Thermit Corporation
A. E. Gibson, The Wellman Engineering Company
J. C. Hodge, The Babcock and Wilcox Company
R. K. Hopkins, The M. W. Kellogg Company
J. B. Johnson, Air Corps, Wright Field
H. L. Miller, Republic Steel Corporation
H. M. Priest, United States Steel Corporation
W. P. Roop, Lieut. Comdr., Brooklyn Navy Yard
W. G. Theisinger, Lukens Steel Company
E. E. Thum, Editor, Metal Progress
W. L. Warner, Watertown Arsenal

IV. High-Alloy Steels—T. H. Nelson, *Chairman;* Metallurgical Research Laboratories

Members:

L. C. Bibber, Carnegie-Illinois Steel Corporation
E. C. Chapman, Hedges-Walsh-Weidner Company
E. Forker, Blaw-Knox Company
A. C. Johnson, Downington Iron Works
V. N. Krivobok, Allegheny Steel Company
T. R. Lichtenwalter, Republic Steel Corporation
D. L. Mathias, Metal and Thermit Corporation
G. A. Maurauth, Maurauth, Incorporated
H. L. Maxwell, E. I. du Pont de Nemours and Company
N. L. Mochel, Westinghouse Electric and Manufacturing Company
R. D. Thomas, R. D. Thomas Company
C. R. Vincent, Jr., Alloy Fabricators, Incorporated
Joseph Winlock, Edward G. Budd Manufacturing Company

V. Aluminum Alloys—G. O. Hoglund, *Chairman;* Aluminum Company of America

Members:

C. L. Hibert, Consolidated Aircraft Corporation
G. H. Moore, Jr., Newport News Shipbuilding and Dry Dock Company
Navy Department, Bureau of Construction and Repair
H. E. Rockefeller, Linde Air Products Company
E. W. P. Smith, The Lincoln Electric Company

VI. Copper Alloys—D. K. Crampton, *Chairman*, Chase Brass and Copper Company

Members:

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C. H. Jennings, Westinghouse Electric and Manufacturing Company
H. C. Jennison, The American Brass Company, Inc.
J. F. Lincoln, The Lincoln Electric Company
W. C. Reed, General Electric Company, Pittsfield, Mass.
J. R. Sproat, Wm. B. Scaife & Sons Company
R. A. Wilkins, Revere Copper and Brass, Incorporated

VII. Nickel Alloys—O. B. J. Fraser, *Chairman;* The International Nickel Company

Members:

F. E. Bush, Driver-Harris Company
J. H. Deppeler, Metal and Thermit Corporation
J. R. Freeman, Jr., The American Brass Company
J. B. Johnson, Air Corps, Wright Field
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G. H. Moore, Jr., Newport News Shipbuilding and Dry Dock Company
Navy Department, Officer-in-Charge, Specifications Section, Design Division, Bureau of Engineering
W. G. Theisinger, Lukens Steel Company
C. R. Vincent, Jr., Alloy Fabricators, Incorporated

Functional Committees

A. Methods of Testing—M. F. Sayre, *Chairman;* Union College

Members:

A. V. deForest, Massachusetts Institute of Technology
R. K. Hopkins, M. W. Kellogg Company
F. M. Howell, Aluminum Company of America
C. H. Jennings, Westinghouse Electric and Manufacturing Company
H. C. Jennison, American Brass Company
A. B. Kinzel, Union Carbide and Carbon Research Laboratories
C. E. Loos, American Bridge Company
H. V. B. Madsen, Navy Department
W. L. Warner, Watertown Arsenal

B. Analysis of Weld Failures—(Chairman not yet named)

C. Weld Stresses—Causes and Effects—Everett Chapman, *Chairman;* Lukenweld, Incorporated

Members:

Rupert Eksergian, Edward G. Budd Manufacturing Company
J. Den Hartog, Harvard University
A. B. Kinzel, Union Carbide and Carbon Research Laboratories
A. Nadai, Westinghouse Electric and Manufacturing Company
M. F. Sayre, Union College
Joseph Winlock, Edward G. Budd Manufacturing Company

D. Non-Destructive Tests—J. T. Norton, *Chairman;* Massachusetts Institute of Technology

Members:

C. W. Briggs, Naval Research Laboratory, Anacostia Station
O. R. Carpenter, The Babcock and Wilcox Company
A. V. deForest, Massachusetts Institute of Technology
H. H. Lester, Watertown Arsenal
N. L. Mochel, Westinghouse Electric and Manufacturing Company

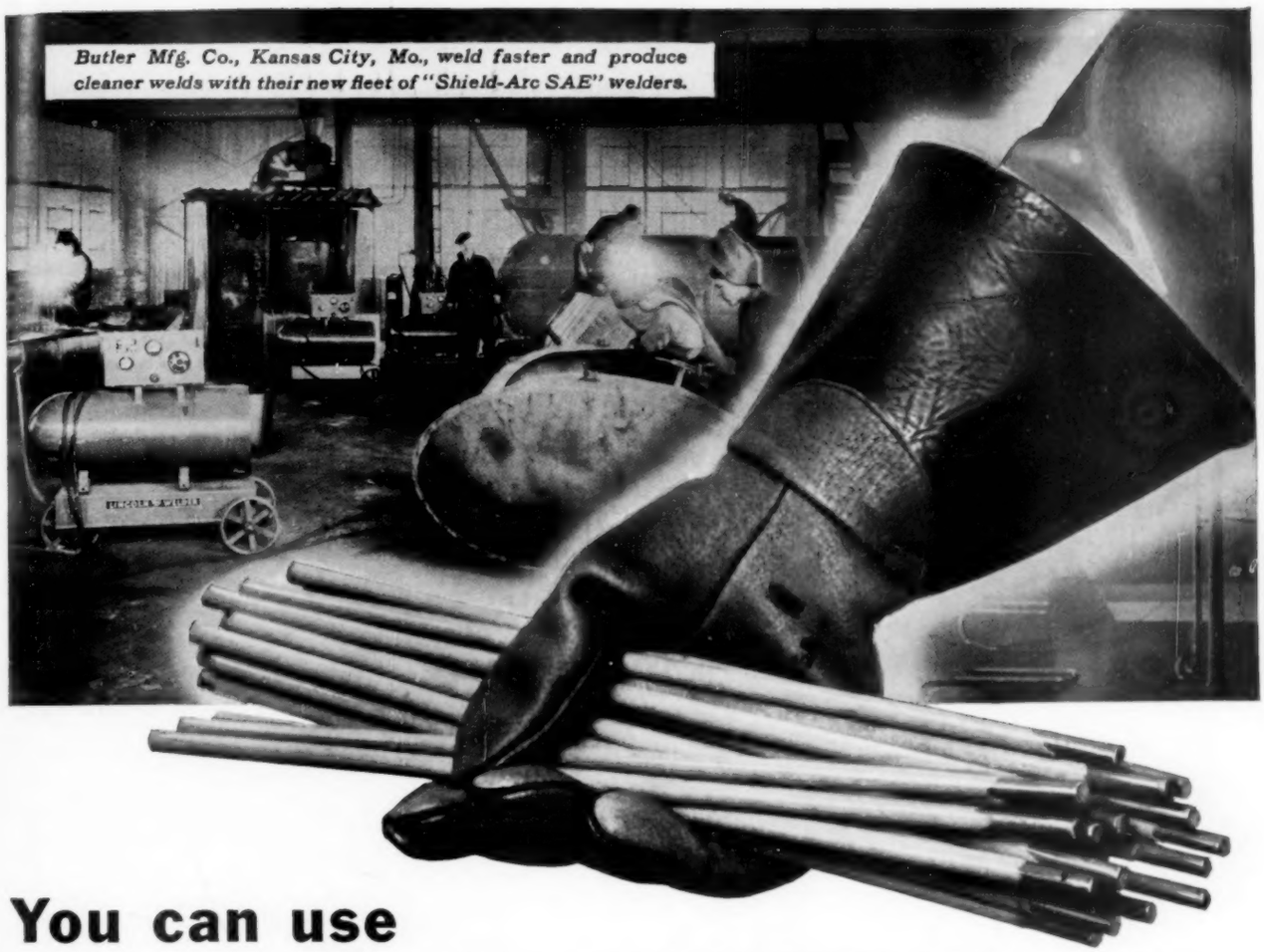
E. Resistance Welding (Chairman not yet named)

F. Fatigue Testing (Structural)—Jonathan Jones, *Chairman;* Bethlehem Steel Company

Members:

L. C. Bibber, Carnegie-Illinois Steel Corporation
H. C. Boardman, Chicago Bridge and Iron Company
A. W. Carpenter, New York Central Railroad
Everett Chapman, Lukenweld, Incorporated
F. H. Frankland, American Institute of Steel Construction
LaMotte Grover, Missouri Pacific Railroad
S. C. Hollister, Cornell University
Walter Hopkins, Maryland Highway Department
Otis E. Hovey, Consulting Engineer, 71 Broadway, New York
Leon S. Moisseiff, Consulting Engineer, 99 Wall Street, New York
H. H. Moss, Linde Air Products Company
G. H. Tinker, New York, Chicago and St. Louis Railroad
A. B. Wilder, University of Illinois
W. M. Wilson, University of Illinois

G. F. JENKS
Colonel, Ord. Dept., U. S. A.
Chairman, Industrial Research Division



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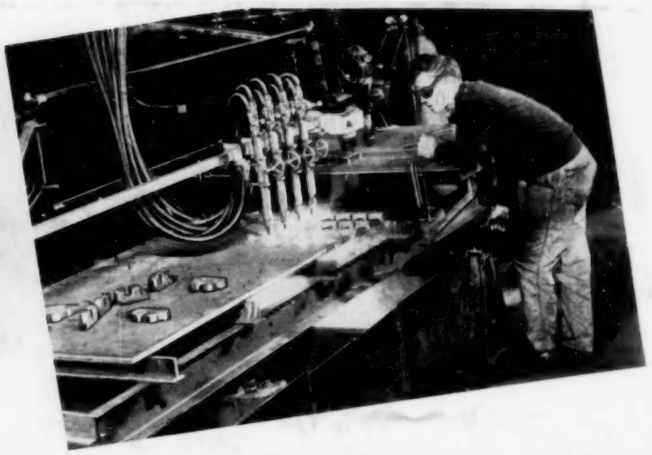

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